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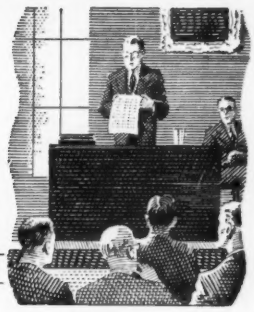

Application of Automatic Control to the Coal Feed of Steelworks Gas Producers

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SYNOPSIS

A description is given of an automatic control system for regulating the coal feed of a steelworks gas producer, which is operated by a combination of blast steam pressure and gas offtake temperature. This system, specially developed to meet the needs of the steel industry, overcomes some disadvantages of previously published methods and is shown to be the simplest and cheapest means of keeping both the gas offtake temperature and the gas quality sufficiently constant under conditions of rapidly varying load.

A satisfactory compromise has been found which prevents serious disturbances due to load changes or to other factors, such as ashing, and yet does not make the combined system so sensitive to temperature that flaring in a rotating producer could build up a lop-sided fuel bed. A method of applying the system to a bank of producers is suggested. It is shown that a system operated mainly by load becomes more efficient with deeper fuel beds. Two types of coal feed regulator gear for Morgan producers are described.

With the advent of automatic control of open-hearth furnaces,[1] the need for automatic control of coal feed to steelworks gas producers has increased, for two reasons: (i) the larger and more rapid changes in gas demand caused by automatic control of furnace roof temperature make manual adjustment of producer coal feed more difficult, and (ii) the full benefit of automatic control at the furnace cannot be obtained unless the furnace is supplied with gas of fairly constant quality.

Automatic control systems for the coal feed of gas producers have recently been developed[2] which keep the gas offtake temperature and gas composition within sufficiently narrow limits, even when the load is subject to large and rapid fluctuations. These objectives were achieved without the use of methods so sensitive to gas offtake temperature that the presence of a flare in a rotating fuel bed could set up cyclic surges of coal feed and thus build up a lop-sided fuel bed.

In a steelworks, the load fluctuations are such that regulation of the coal feed rate by gas offtake temperature alone is hardly adequate, and some form of load measurement is required which is capable of changing the coal feed rate immediately a change in load occurs. This has been shown to give much better control than systems operated by gas offtake temperature only.

A system operated by the blast air flow has already been described,[3] but as it required an air main and orifice for each producer, the development of a simplified version was necessary before it could be recommended for general use in steelworks. The use of blast steam pressure as a measure of load had been tried as a simpler alternative, but the variable relationship between air flow and steam pressure made this system difficult to set up and maintain in efficient operation.

Further experimental work on an 11-ft. Morgan producer at the Kilnhurst Steelworks of John Baker and Bessemer Ltd., with the collaboration of George Kent Ltd., has solved these problems to the extent that dependable and reasonably simple methods, specially suited to steelworks conditions, are now available.

MEASUREMENT OF LOAD ON A PRODUCER

Direct flow measurements of hot, raw producer gas are subject to considerable practical difficulty, and orifices in gas mains are liable to give false readings, owing to deposits of tar and soot; they also cause

Manuscript first received 2nd April, 1952, and in its final form 14th July, 1952.

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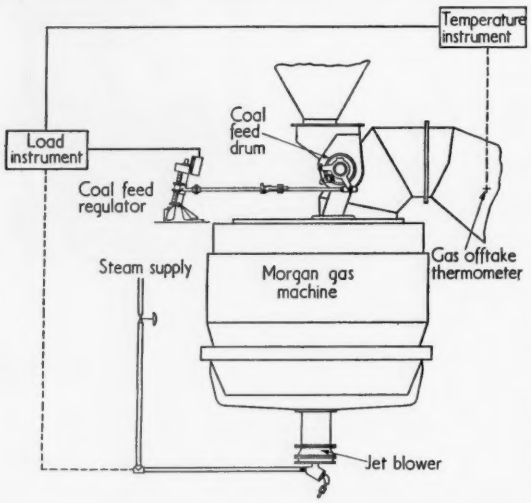


Fig. 1—Diagram of layout for control by load (steam pressure)

undesirable pressure drop. Brick Venturis are probably easier to keep clear of deposits, but are comparatively more expensive to insert in existing mains.

With a battery of producers the flow from each gas machine would, for control purposes, have to be measured independently, and it would usually be difficult to find a length of straight main, between each producer and the common gas main, suitable for inserting an orifice or Venturi. The calibration of such an orifice or Venturi would, in any case, change continually with variations of gas temperature or composition. Measuring the flow of blast air is much simpler and more reliable, and in some ways is more logical since it permits the direct proportioning of coal to air. In producers with steam turbo-blowers or electric fan blowers a sufficient length of blast main usually exists already, between the blower and the blast hood, and the insertion of an orifice into this main would give a sufficiently reliable measure of load. Alternatively, a cone could be fitted on the air inlet to the blower.

In Morgan producers with jet blowers immediately below the blast hood, there is no length of blast main and there is little room to add a cone inlet beneath the blower. For experimental purposes a horizontal length of air main was added.[3] This was very satisfactory, and the small pressure drop did not affect the performance of the jet blower; but the space taken up by the main, and its cost, discourage its general use.

There are several methods of indirectly obtaining a measurement of load. The speed of a turbo-blower or motor-driven fan, the steam flow to the former, or the current taken by the latter, might be used; or the pressure drop across the fuel bed could be measured. Most of these methods, however, have serious practical objections.

One indirect method that has now been successfully developed for purposes of automatic coal feed regulation is the measurement of steam pressure to the jets of a Morgan blower. This is not so reliable a measure of load as is air flow rate, but it is easily applicable to most steelworks producers.

CONTROL BY STEAM PRESSURE

The general arrangement of this system is shown in Fig. 1. The pressure of the steam at the jet blowers is made to operate a load instrument, which passes a signal on to the coal feed regulator. When a sudden change of load occurs, the coal feed rate varies by a proportionate amount without waiting for any change of gas offtake temperature. By this means, fluctuations of gas offtake temperature and gas quality can be largely prevented. A supercharge action[3] was employed in all the tests described in the present paper.

The temperature instrument responds to a thermocouple in the gas offtake, and is linked with the load instrument in such a way that their corrective actions are added. The primary function of the temperature instrument is to compensate for disturbances not due to load changes. Some of these disturbances, such as changes of coal quality or moisture content or variations in fuel bed depth, alter the desired coal/air ratio, whilst other disturbances, such as the packing of fine coal inside the feed drum compartments, may reduce feeder capacity and so necessitate a different rate of feeder rotation for a given load. Such disturbances not due to load changes usually occur gradually, so that the necessary corrections need not be rapid. The use of steam pressure instead of air flow as a measure of load, however, introduces other errors which do require immediate compensation.

Figure 2 shows calibrations, at different dates, of air flow against steam pressure; considerable alterations take place. These differences were found to be caused mainly by changes in pressure drop through the fuel bed, possibly caused by fine coal, wet ash, changes in blast saturation temperature or ash fusion temperature, or changes in bed depth, but they may also be partly the result of blocking or erosion of steam nozzles.

The change in coal feed rate caused by a change in steam pressure is governed by the proportional setting on the load instrument, and if this setting is at the correct value corresponding to the steam-pressure/air-flow calibration existing at the time, good control can be obtained as shown in Fig. 3*a*, where the load changes are indicated by the record of air flow rate to

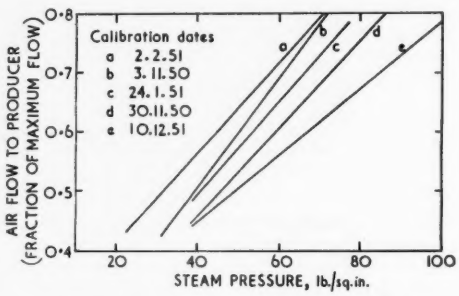


Fig. 2—Variations in relationship between steam pressure and air flow to producer

the blast. If the load instrument setting does not match the prevailing steam-pressure calibration, however, the coal feed is not adjusted by the right amount when a load change occurs, and bad control results (*see* Fig. 3*b*). To achieve good control in this case, the temperature instrument must also be able to make a substantial correction rapidly. If the load instrument then fails to make the correct coal feed adjustment, and a temperature deviation therefore starts, the remaining adjustment would be made as quickly as possible by the temperature instrument.

CORRECTION OF LOAD INSTRUMENT BY TEMPERATURE INSTRUMENT

The use of derivation action[4] in the temperature instrument, so as to give a rapid response, enables a strong signal from it to be applied to the load instrument.[2] A compromise has to be made, however, which will apply a sufficient corrective action without making the temperature instrument too sensitive to cyclic flaring fluctuations and thus creating a lop-sided fuel bed. Tests were necessary to find whether this compromise was possible, and to determine the best instrument settings.

It was not practicable to create variations of fuel bed conditions that would give a series of different relationships between steam pressure and air flow; it was easier to alter the proportional setting on the load instrument and to carry out a series of tests over a short period, during which the steam-pressure/air-flow calibration might be assumed to remain unaltered.

Since the effectiveness of any control system depends on the amount of load fluctuation to which it is subjected, the variance line technique was used to assess the results. In this technique, which has already been fully described[2] and used for evaluating other control systems,[3] the ratio of temperature fluctuations to load fluctuations is measured; the greater the slope of the variance line, the worse is the control. The results are given in Fig. 4. Curve *a* represents an insensitive temperature system, whilst curves *b* and *c* are the result of using a strong temperature correction. Curve *c* represents the most sensitive temperature setting that it is safe to use on a Morgan producer without causing a lop-sided fuel bed, as calculated from past experience.[3]

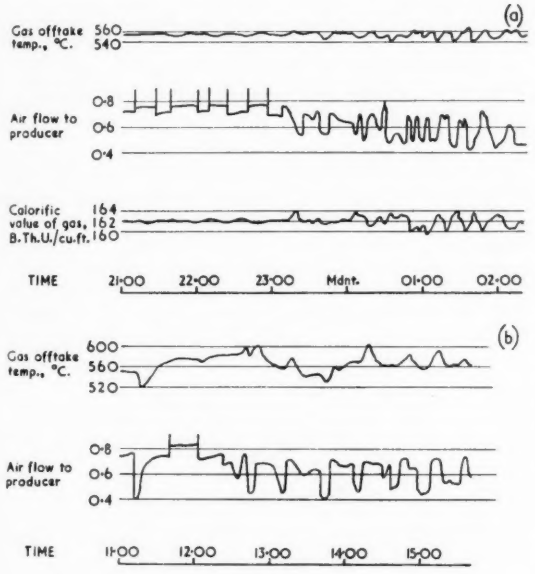
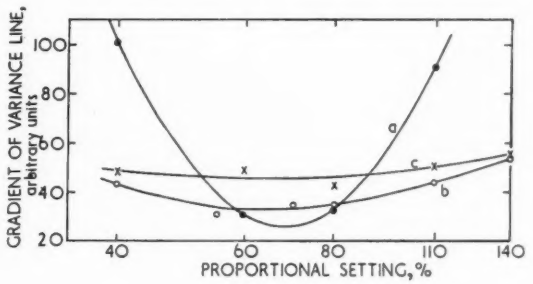


Fig. 3—Part records of tests with right and wrong proportional settings on load instrument. Temperature instrument: Proportional sensitivity 200%; integral time 2·9 min. Load instrument: (*a*) Correct proportional setting 90%; (*b*) wrong proportional setting 40%



Temperature Instrument Settings

	Proportional Sensitivity, %	Integral Time, min.	Derivative Time, min.
a	200	2·9	..
b	30	10	2·9
c	30	10	4

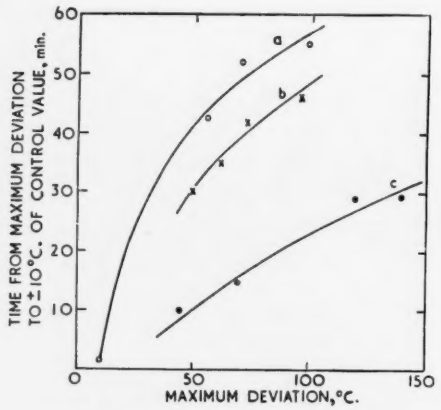
Fig. 4—Effect of load instrument setting on efficiency of systems

The curves show that, at the time of these particular tests, optimum control was obtained with a load instrument proportional setting of about 70%, but on either side of this value the control deteriorates badly if backed up only by an insensitive temperature correction. With such a system the load instrument would obviously have to be set very carefully and readjusted if the plant conditions moved the optimum to a different value. With a sensitive temperature correction, the setting of the load instrument does not have to be so exact and changes in plant characteristics can be ignored; this makes it possible to use steam pressure as a measurement of load. The combined system has great advantages compared with the best possible system of control by gas offtake temperature alone,[3] since the latter gives a variance line slope of at least 100 and is more liable to create a lop-sided fuel bed. Thus, even a faulty load signal serves a valuable purpose, by providing most of the adjustment to the coal feed immediately a load change occurs.

EFFECT OF DISTURBANCES OTHER THAN LOAD CHANGES

Although the major disturbance on the plant was a rapidly varying load, several less frequent disturbances must be considered when assessing the various systems. Some of these occur gradually, as mentioned on p. 258, and the required correction is easily made, but three types of disturbance occur relatively suddenly, and require special consideration:

(i) Those involving a period of manual control or stoppage of the coal feed, mainly comprising dips to



a. Control by load with insensitive temperature signal
b. Control by load with sensitive temperature signal
c. Control by temperature only

Fig. 5—Recovery after shut-down periods

determine ash and fuel bed profiles, and shut-down periods for fettling at the furnace. The control system was unavoidably out of action at such times, and took over again with a temperature deviation that was often quite large. The speed of recovery to within 10° C. of the desired temperature has been taken as a criterion of the efficiency of the system in correcting such disturbances.

Curves *a* and *b* of Fig. 5 are for systems with insensitive and sensitive temperature components, respectively, and curve *c* is for a system of control by gas offtake temperature only. As the temperature signal becomes more predominant, the rate of recovery increases; investigation of chart records, however, showed that the fast recovery rate was obtained with extreme coal feed rates, thus upsetting the normal proportion of distillation products in the gas and giving a calorific value far from the average. Since constancy of calorific value, as well as of gas offtake temperature, is the aim of automatic control of coal feed, some compromise is required, and it is considered that the temperature instrument settings given for curves *b* and *c* in Fig. 4 would probably suit most cases.

(ii) The second type of disturbance is that which involves no interruption of the coal feed rate or of the operation of the control system, a temperature deviation being caused by a change in the state of the fuel bed. Careful study of the charts showed that the only disturbance in this category that was not easily corrected was that due to ashing. On a Morgan producer, ashing is an intermittent process carried out when warranted by the depth of the ash bed, and usually occurring once per shift. Ashing causes a general loosening of the fuel bed and can result in the formation of cavities and flares, which are then collapsed by manual poking. The ashing operation does not involve interruption of the coal feed, and any temperature disturbance is likely to be mainly the result of flaring.

No appreciable deviation of temperature at the ashing period was shown during the previously reported tests on systems of control by temperature only, and it was therefore concluded that any temperature disturbance due to flaring was being counteracted quickly by a readjustment of the coal feed rate.

With systems of control by load, the controller reaction to a temperature disturbance was much slower, and it was therefore possible to study, over a number of ashing periods, the effect of the ashing operation on the gas offtake temperature. It was concluded that, in general, the effect was a minor one and only a slight temperature rise was obtained. On some occasions, however, there was a much greater rise in temperature, and visual inspection of the fuel bed then showed the presence of at least one large flare. After poking down by the operator to collapse the channel, the temperature returned to near its control value.

Certain plant managers hold the view that temperature deviations due to flaring should be visible on the record, and in this respect the system of control by load shows an advantage. But with only a slow integral[4] signal from the temperature instrument, care has to be taken that this deviation does not start an integral hunt, an example of which is shown in Fig. 6*a*. With a more sensitive temperature system (Fig. 6*b*) this was avoided, although the disturbance caused by flaring was still visible. The system of control by load in conjunction with a fairly sensitive temperature signal again provides a good compromise.

When controlling from steam pressure, it might be thought that the general loosening of the bed would cause a variation in the relationship between steam pressure and blast air flow. However, calibration showed that the relationship varied only slightly and that the original value was restored within a few minutes. Temperature deviations from this cause were not significant, in comparison with those caused by flaring.

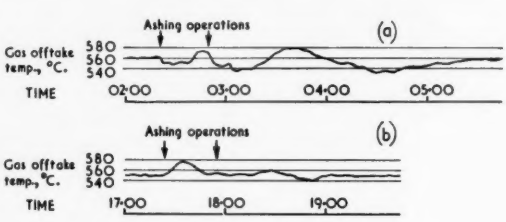
(iii) A third type of disturbance is one caused by limitations in the coal feed mechanism. At times during the tests, when the load changes were unusually severe, the control mechanism would attempt to impose a feed rate greater than was possible with the existing feed gear. A temperature deviation would therefore result, and again this would be corrected more rapidly by the more sensitive temperature systems.

Also, the coal feed regulating gear on other plants might be less suitable because (*a*) the relationship between control signal and coal feed rate is not linear, or (*b*) the relationship is liable to change suddenly, or (*c*) the regulating gear is too slow in its response to a control signal. A strong signal from the temperature instrument would then ensure the maintenance of effective control.

In general, a system of control by load prevents temperature deviations arising from normal load changes, but a strong temperature signal is required to correct deviations that can arise from so many other causes.

COAL FEED REGULATOR GEAR

The function of the regulator gear is to convert an air-pressure signal from the controller into a rate of coal feed to the producer. In common with other producers, a Morgan gas machine has no method of directly measuring the rate of coal feed, but the feed rate is regulated by altering the speed of revolution



	Load Instrument	Temperature Instrument		
	Proportional Setting, %	Proportional Sensitivity, %	Integral Time, min.	Derivative Time, min.
a	100	200	2·9	..
b	110	30	10	4

Fig. 6—Temperature disturbance due to ashing

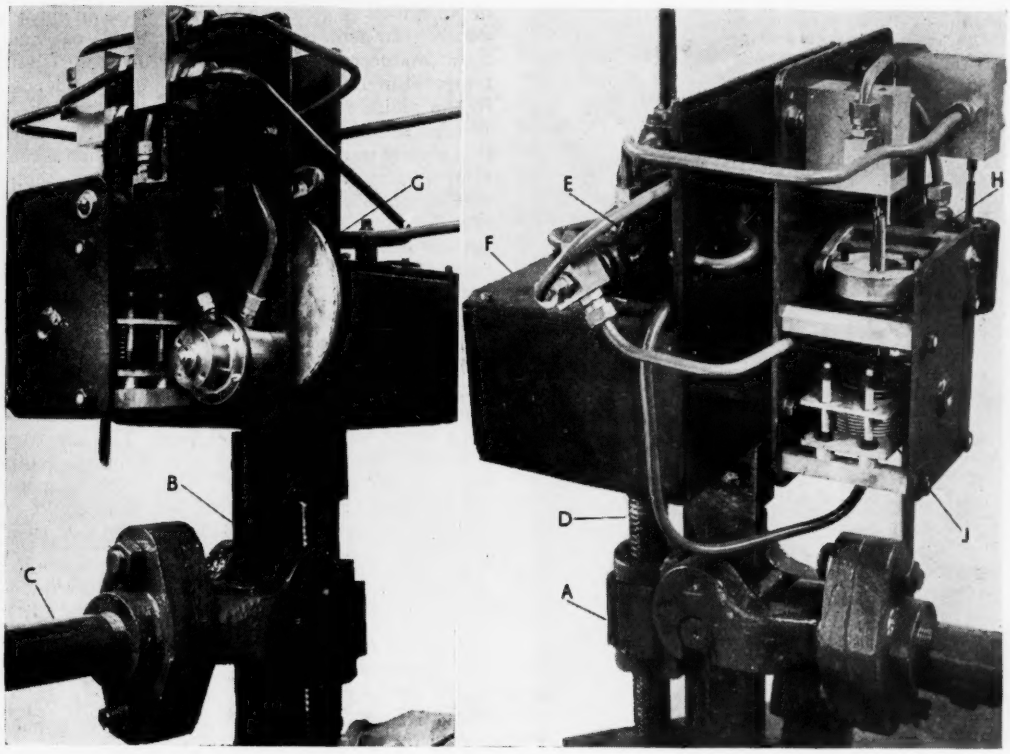


Fig. 7—Coal feed regulator (air-motor system)

of the feed drum. In practice, the relationship between drum speed and coal feed rate varies according to the size-grading of the coal and the degree of tar and dust build-up in the drum compartments. This was accepted (*see* p. 258) as a disturbing factor, for which the control system had to be capable of making the desired compensation.

These tests have shown that the main requirements of a regulator gear, to give an efficient control system, are:

(i) Consistent characteristics: the relationship between drum speed and control pressure should not vary, as otherwise additional disturbances are introduced into the system. In particular, there must be no ' dead zone ' or lost motion in the assembly. A response must be obtained for any small change in control pressure

(ii) Rapid response to a change in control pressure, since time-lags relating to application of coal feed must be kept to a minimum

(iii) Wide range of operation, with the lower limit at zero feed.

Other desirable characteristics are:

(iv) Low cost and ease of manufacture; construction from standard parts as far as possible

(v) Reliability

(vi) Low rate of wear and low maintenance costs

(vii) Easy convertibility to hand control in the event of failure.

In addition, it must be remembered that the regulator will usually be operating in an atmosphere including much dust and tar, and that the load on the feed drum drive is highly variable because of the tar and dust deposition on the drum and in the bearings.

The method of feed regulation used during the tests is illustrated in Fig. 7. Control of the speed of the feed drum is obtained by altering the height of a crosshead A, moving on an oscillating quadrant arm B; this varies the length of the stroke of the driving rod C. The drum rotates by the action of friction blocks, which are engaged on the whole of the driving stroke and are automatically released on the return stroke. The height of the crosshead is adjusted by a screw D, turned by a reversible compressed-air motor E through suitable gearing F. The air supply to the motor is controlled by a positioner mechanism linked to the quadrant screw through the rotary cam G and forked crank H. The control pressure is fed into the bellows J.

The positioner mechanism incorporates primary and secondary pilot valves. The effect of this is to give a very sensitive ' trigger ' action, so that a change of as little as 0·1 lb./sq. in. in control pressure is sufficient to permit the necessary large volumes of compressed air to be admitted to the motor, resulting in a movement of the screw and rapid repositioning of the crosshead. A fuller description of this feed regulator has been given elsewhere.[2] The system operated for two years on this plant, and little maintenance was required.

With the main object of reducing production costs,

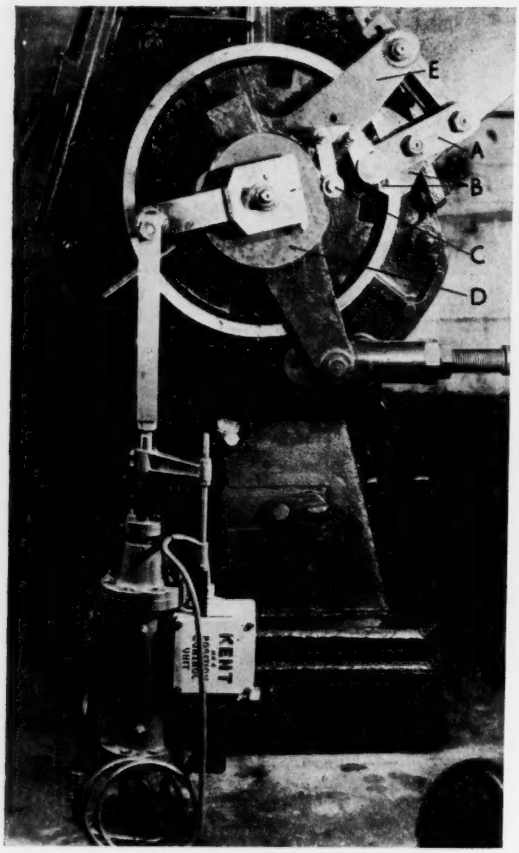


Fig. 8—Coal feed regulator (power cylinder system)

a second method of regulation (*see* Fig. 8) has been devised. The length of the driving stroke is kept constant, but the proportion of the stroke during which the friction blocks are engaged is varied. The link *A* carrying the friction blocks *B* is pinned at its outer end, and if this link is maintained in a radial position the blocks will not grip the drum. When the link is allowed to fall, the shoes grip and the drum is driven. The link mechanism is operated by the action of a roller *C* and cam *D*. The cam does not rotate with the arm *E*, but its position is controlled by a standard Kent power cylinder and position-control unit.

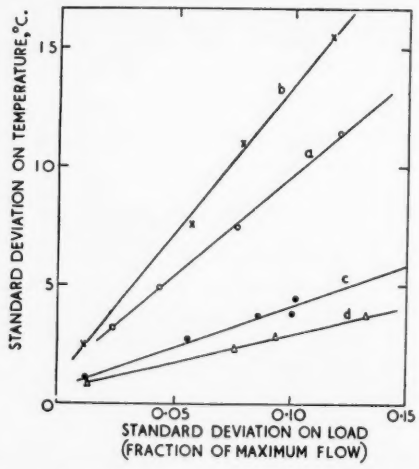
On the driving stroke, the friction blocks do not grip until the roller passes on to the low portion of the cam. The proportion of the stroke during which the blocks are engaged depends upon the position of the cam and therefore upon the control pressure to the position-control unit. This arrangement works satisfactorily and fulfils the requirements mentioned earlier; it has now been in operation for several months under normal plant conditions.

EFFECT OF FUEL BED DEPTH

Increasing the fuel bed depth in a gas producer improves the gas quality,[5] at least up to a critical depth which is greater than that used in most producers. To achieve the best practicable gas quality, most producers would have to be run at a gas offtake temperature not far above that at which tar deposition in the mains might occur or caking in the fuel bed become serious. Operating with this narrow margin, it is clearly important to prevent wide fluctuations of temperature. The close control of gas offtake temperature by automatic means thus makes possible the use of deeper fuel beds than have hitherto been customary. On the other hand, with lower gas offtake temperatures the thermal time-lags and heat-exchange rates of the fuel bed will probably be altered, and it becomes important to see whether automatic control still functions well under such conditions.

The two main types of automatic control system, *i.e.*, by gas offtake temperature alone and by a combination of load and offtake temperature, were both tested with deeper fuel beds. The effectiveness of control was tested in each case at offtake temperatures of 580° C. and 520° C., the latter temperature representing the deepest fuel bed that could safely be accommodated in the Morgan producer used for the tests.

The results are shown as variance lines in Fig. 9. Lines *a* and *b* represent the best system of control operated by gas offtake temperature alone,[3] whilst lines *c* and *d* represent the best system operated by load, with temperature reset. This graph not only shows the superiority of control mainly by load, compared with control by temperature only, but also demonstrates that when the offtake temperature is kept at 520° C., instead of 580° C. (*i.e.*, the bed is deepened), the effectiveness of control deteriorates from *a* to *b* in one case, but actually improves further, from *c* to *d*, in the other case. The reasons for this are somewhat involved and have been discussed



	Control Temp., ° C.	Load Instrument: Proportional Setting, %	Temperature Instrument: Proportional Sensitivity, %	Temperature Instrument: Integral Time, min.	Temperature Instrument: Derivative Time, min.
a	580	..	20	10	4
b	520	..	20	10	4
c	580	90	200	2·9	..
d	520	90	200	2·9	..

Fig. 9—Effect of fuel bed depth on efficiency of control systems

elsewhere.[2] The greater relative efficiency of control by load, when using deeper fuel beds, is a strong point in favour of this system. In combined systems, such as those described in this paper, the aim should be to make the load instrument supply as much of the regulating action as possible.

It may be of interest to note that the Morgan producer at John Baker and Bessemer's Kilnhurst steelworks, on which these tests were carried out, has been working for several months with a gas offtake temperature of 540° C.

ENRICHMENT OF GAS DURING CHARGING PERIODS

During the charging period of an open-hearth furnace, the flow of gas is at the maximum possible rate until the roof temperature reaches the value at which gas flow has to be reduced; hence the only way of increasing the melting rate is to increase the calorific value of the gas or its tar content.

In so far as the high gas demand at a particular furnace causes an increased load on the producers, this gas enrichment can be provided by the automatic control system used for regulating the producer coal feed. The response to load changes is so arranged that when the gas demand is high the coal feed rate is increased to a slightly higher value than is necessary to replace the fuel consumed. This causes a slow rate of increase in the depth of the fuel bed and a slight fall in gas offtake temperature. The surplus coal yields additional distillation gases and tar, thus enriching the producer gas and increasing luminosity. The effect persists only while the load remains above its average level; during the furnace refining period, the gas demand is reduced and the opposite effects occur at the producer.

These changes in gas offtake temperature at the producer can be permitted, since they are of such a size and direction that they tend to keep the temperature at the cooler end of the gas main more constant and may thus actually help to reduce any tendency to tar deposition. The temperature drop along the gas main is greatest when the gas flow is at its lowest value, and at such times the temperature at the cooler end falls unless the temperature at the producer end is raised by an appropriate amount.

The system described above is more likely to be of benefit where one furnace only is being supplied with gas. With more than one furnace the load is likely to be averaged, as a result of one furnace charging while another is refining. For this reason the system has not been taken beyond the stage of preliminary tests. Two methods have, however, been tried out in practice, and a brief description of these has been given elsewhere.[2]

APPLICATION TO A BANK OF PRODUCERS

In applying automatic regulation of coal feed to several producers in a battery, the method used to measure load becomes a crucial factor. If air flow is used to operate the load instrument, it is probable that each producer in the bank would require its own load instrument and temperature instrument. If blast steam pressure can be used, however, as described on p. 258, some simplification and economy might be possible with banks of producers. If all the producers in a bank are supplied with steam from a common main, the steam pressure in that main being regulated by a single controller so as to keep a constant gas pressure in the gas main, then a single load instrument, operated by the pressure in the common steam main, might serve the whole bank of producers.

One essential condition would be to ensure that no appreciable interference with the steam pressure at the jet blower of any producer could be caused by manual manipulation of a valve between the steam main and the jet blower, for in that case the controlled pressure in the steam main would no longer represent the load on that particular producer. To compensate for varying fuel bed and other conditions in individual producers, each would still have to be supplied with its own temperature instrument, operated by the temperature in the gas offtake. The system used up to the present, in which the signal from this temperature instrument was fed into the load instrument of the same producer, would no longer be possible where there are several temperature instruments and only one load instrument. Instead, it is suggested that a Summator Unit might be used, at each producer, for adding the individual temperature signals to the common load signal. A detailed description of such a unit, used for supercharging purposes, has been given elsewhere.[2]

A further simplification is possible in which the signal from the gas-main pressure controller, as used to adjust the regulating valve on the common steam main, is directly fed as a load signal to the summators on the various producers. This procedure is probably not to be recommended, however, since the signal required to operate the steam valve cannot be taken as a reliable measure of load.

CONCLUSIONS

(1) Automatic regulation of the coal feed on a steelworks producer, so as to maintain reasonably constant gas offtake temperature and gas quality, is possible without any risk of flaring causing a lop-sided fuel bed. To do this it was found necessary to have two control instruments, one actuated by load and the other by gas offtake temperature.

(2) The load instrument should measure air flow rate if possible, but, if this is not convenient, good control can be obtained on Morgan gas machines by making the steam pressure to the jet blower operate the load instrument.

(3) The settings of the instrument operated by gas offtake temperature should be made sensitive enough to counteract disturbances such as ashing, and also to compensate for imperfections either of the load measuring system or of the coal feed regulator. The settings must not, however, make the temperature instrument too sensitive to flaring. A satisfactory compromise giving good overall control is possible, but the optimum settings depend on individual plant characteristics.

(4) Such a combined system has the advantage that it can be set up without elaborate plant calibrations, and will continue to operate satisfactorily in spite of changing plant characteristics.

(5) Using such a system it is possible to lower the gas offtake temperature appreciably, without increasing the danger of depositing tar in the mains; this enables deeper fuel beds to be used. The use of automatic control gives a more uniform gas quality in any case, but by operating at a lower offtake temperature the general level of gas quality can be improved. Control systems actuated mainly by load become more effective with deeper fuel beds.

Acknowledgments

This work forms part of a programme of research on steelworks gas producers, carried out by the British Coal Utilisation Research Association with the support of the British Iron and Steel Research Association, whose assistance and encouragement is gratefully acknowledged.

The authors wish to acknowledge the collaboration they have received throughout this research from the staff of George Kent Ltd., and in particular from Mr. J. Grout and Mr. M. A. Beechey. Thanks are also due to George Kent Ltd. for the loan of much of the control equipment used in the tests.

Acknowledgment is due to Mr. Sidney E. Baker, whose invention of the air-motor system of feed regulation was one of the chief factors leading to the success of the tests, and to Mr. Robert C. Baker for so unreservedly placing the producer at the authors' disposal and for providing such excellent facilities for research.

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A Recent Development in Ingot Chariot Control

By A. W. Leadbeater, A.M.I.E.E., and R. B. Jamieson

SYNOPSIS

The paper describes a successful solution to the problem of damage to an ingot chariot system caused by ingots falling on to the running track and collector rails. The deep, concrete-lined trench with its four collector rails has been replaced by a single flexible trolley wire situated in a shallow trench beside the track, and contactor reversers with control rectifiers have been mounted on the chariot. Maintenance costs are greatly reduced, and repairs, where necessary, are simplified.

STATEMENT OF THE PROBLEM

The underlying principle in good maintenance is anticipation: periodic inspections and the work done during normal shut-downs all aim at keeping the plant in operation. Certain sources of nuisance that cause undue concern to both maintenance and production personnel often stand out from the maintenance programme. Although the original planners may be criticized, these nuisance factors are often due to a sound engineering scheme that has failed under abnormal conditions of work. These failures are difficult to rectify, since some are tied by orthodox requirements of a system control; to overcome them, new ideas have to be introduced and long-established methods discarded.

Since other plants may be experiencing similar troubles, it was decided to record the steps taken to deal with such a source of trouble on the plant with which the authors are associated. The maintenance costs and production loss on the ingot chariot had become prohibitive, so that steps had to be taken to overcome this nuisance.

The ingot chariot links the soaking pits with the 40-in. mill approach rack ; an end view is shown in Fig. 1. It has a maximum travel from No. 12 pit to the mill of 135 yd., which it covered in an average time of 60 sec.

The chariot runs on a rail track that divides the pits from the ingot-stripping section; as ingots are stripped, they are lifted, carried over the chariot track and placed in the pits for soaking. They are lifted to a time schedule and placed in the chariot chair in a slightly forward off-vertical position. The chariot

Manuscript received 4th June, 1952.

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then travels down to the mill, where at the final approach the chair is tilted by a ramp, depositing the ingot on the rack; the chariot then returns to the pit that is being unloaded.

After stripping, the ingot is lifted by overhead tongs and must be carried over the chariot track twice before being accepted by the mill. This period of crossing the chariot track is the source of the trouble: ingots are dropped during lifting and transit, and the damage to the chariot track has been great. The effect this had on the running of the machine can be seen from Fig. 2, which is a sectional view showing the original arrangement of the chariot, the running rails and electrical pick-up rails. The foundations of the running rails consisted of reinforced concrete walls, which formed a trench to carry the electric conductor system on the side of the wall opposite the pits.

Normally this could be called a sound engineering job, but its apparent soundness defeated its purpose. It was rigid and strong but not durable; the average ingot, weighing about 4½ tons, can cause much damage when dropped from a height of 12 ft. Many attempts were made to strengthen these foundations, heavy steel joists being incorporated, but absolute rigidity was not the answer. The shock was transmitted down into the foundation, and the side walls crumbled, allowing distortion of the top running rails with consequent damage to the electrical collectors; ingots were often dropped into the trench, involving much loss of time in replacement and repairs. Apart from this, the trench itself was a collector of mill scale, involving considerable labour at intervals for cleaning out.

It seemed possible to solve the problem by filling in the trench and finding alternative means of supplying power to the chariot, but the latter presented a major obstacle. The chariot requires only one power motion, long travel, which is obtained from a D.C., 50-h.p., 250-V. series motor; the control is remote, being located at the mill pulpit. Four collector rails are required to obtain forward and reverse motion with acceleration steps. These are necessary to

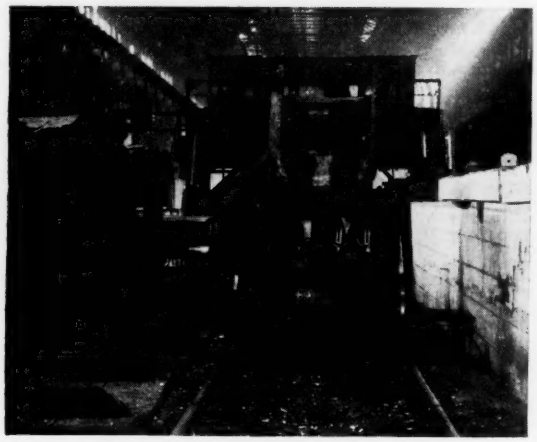

Fig. 1—Ingot chariot, showing soaking pits on left, collector wire in trench at base of wall with stripping bay on far side

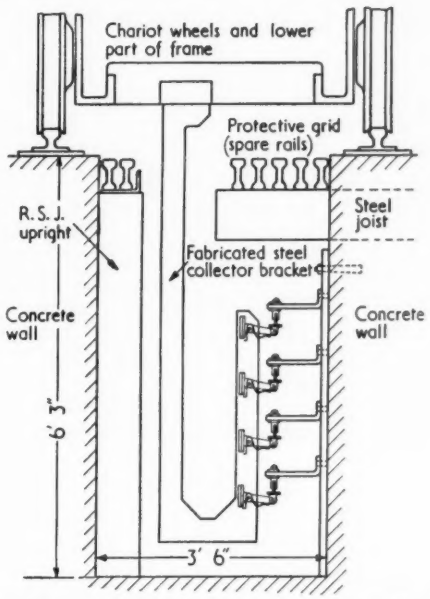

Fig. 2—Section of chariot trench, showing collectors, T-bars, and brackets originally installed

obtain the relative polarity between series field and armature, the acceleration resistances and contactor gear being next to the pulpit. Merely to lift out these conductors and re-position them above ground level was to invite more trouble in the form of smashed rails; in any case, a flexible pick-up was indicated after experience with the erratic motion of the chariot when running on damaged rails. Trolley wires would give this flexibility, but the accommodation of four down-shop wires was impracticable. Attention was focused on the possibility of using a single trolley wire to carry the necessary power and control to the chariot, as it was considered practicable to protect a single wire. At this stage no one was sure whether a series motor could be suitably controlled by a single wire; to the authors' knowledge no such scheme was in existence at the time.

It appeared obvious that contactor reversers would have to be mounted on the chariot itself to obtain the relative change between field and armature; furthermore, a method of selection was required. Although it might have been possible to use electronic means to obtain this control, such schemes were not fully investigated, since it was considered essential to retain a simple robust system capable of withstanding the dirty and arduous mill duty. A simple scheme was evolved in which metal rectifiers were used for selectivity in such a way that by changing the power polarity to the machine the desired relation between armature and field was obtained.

SCHEME A

The original scheme, shown in Fig. 3*a*, allowed experiments to be carried out on the shop floor with a 'test rig' made up from a series motor and contactor gear. It was found that unorthodox schemes could be made to work, and sufficient confidence was gained

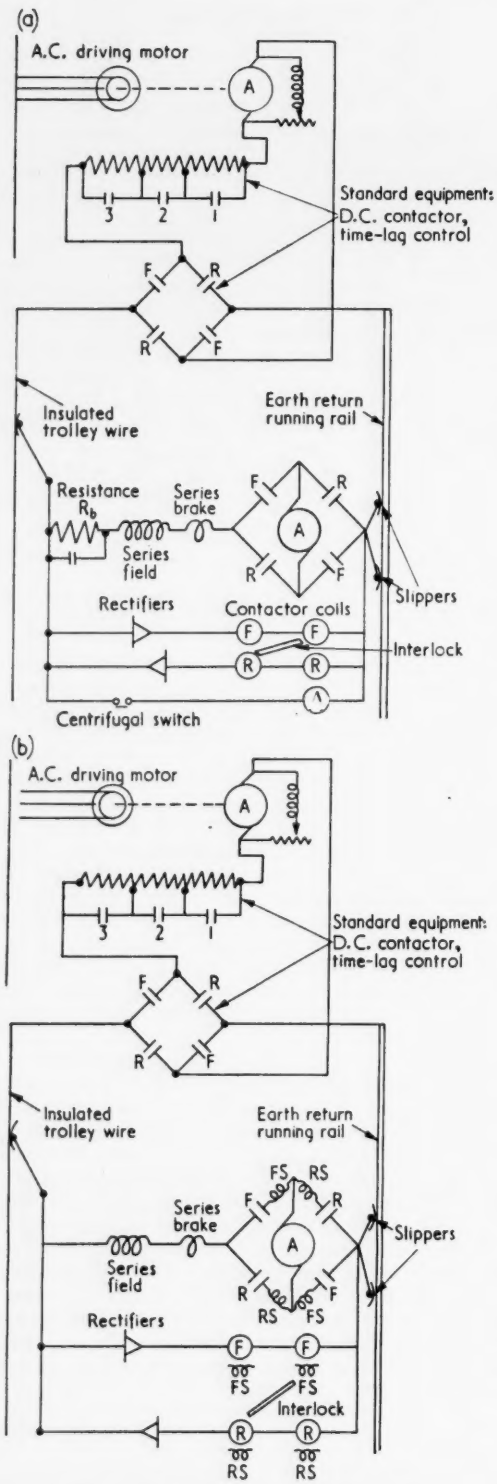

Fig. 3—(*a*) Scheme A ; (*b*) Scheme B

to warrant making up a full-scale model, which was installed as a temporary set-up on the chariot itself. Apart from a few teething troubles, the scheme amply justified itself and worked successfully for about ten days without a hitch.

Although it was thought at this stage that more time was needed for running in, the Civil Engineering Department were so concerned at the condition of the running-rail foundations, with the possibility of a mid-week shut-down, that permission was given to fill in the trench and to make a substantial sleeper-ballast running rail. From this point onwards, it was impossible to go back; the chariot was now running successfully with an electrical scheme using a single $\frac{3}{8}$-in. dia. trolley wire for live pick-up, and earth return *via* the steel running rails.

The scheme for the original ' A ' circuit is shown in Fig. 3*a*. The power is obtained from a motor-generator set at 250 V., D.C., which is necessary to allow one leg to be earthed; the polarity of the earthed leg is alternately positive or negative, according to chariot direction. The D.C. generator is shown as a self-excited machine, but excitation can be obtained from the works' 250-V. supply; both systems work successfully.

The control on the pulpit is standard, with a forward and reverse controller with accelerating steps, the contactors and resistances for this duty being housed near the pulpit. The operator can put his controller into either forward or reverse position for ' inching ' on No. 1 notch or, as is usual, hard over, bringing into operation the automatic timers for the accelerating contactors 1, 2, and 3. The effect of this remote control on the chariot operation is as follows: On the first notch the chariot receives a full voltage surge, the polarity depending upon the direction of the master controller. By means of rectifiers this polarity selects the directional contactors. As soon as the contactors close, the voltage at the chariot falls to a value proportional to the starting current and the buffer resistance. The starting current is controlled by the resistors R_1, R_2, R_3, and R_b, and is proportional to the motor generator voltage, which is constant at 250 V. This voltage at the chariot enables the selected contactors F or R to remain closed while the motor picks up speed. As the armature revolves and generates a back e.m.f., a centrifugal switch closes at a predetermined speed, closing contactor Δ, shorting out resistor R_b, and allowing the chariot to attain full speed.

This system was successful; it enabled the running track to be cleaned up and relaid on a ballast track, and the whole mechanical set-up to be brought into line. Without R_b, the contactors in the chariot ' chattered ' until the back e.m.f. was sufficient to hold them in. The resistance value of R_b and starting current I_s were, however, rather critical, and the chariot was a little sluggish in getting away. This led to further investigation, and finally scheme B, a modified version of scheme A, was introduced (*see* Fig. 3*b*).

SCHEME B

This scheme used a standard D.C. contactor, greatly modified; briefly, a series coil was wound on to the

core in line with the shunt coil. This meant doubling the length of the centre core and lengthening the outside yoke to suit; tests were taken of the amp. turns required to hold in the contactor, and these were correlated with the known starting current on the chariot. A complete new control unit was made up (*see* Fig. 4), using the modified contactors, and a change-over was made when convenient. This modification dispensed with the resistance R_b, the contactor Δ, and the centrifugal starter; the only additional gear on the chariot frame was four contactors and two relatively small rectifiers.

In scheme B, owing to this modification, when the operator moves his controller to the first notch, the chariot receives a surge of power at full voltage, which operates the respective F or R contactors, dependent on its polarity. As soon as these contactors close, the voltage drops to zero, owing to the current surge, but the contactors are retained because of the series coils on them, thus allowing the motor to get away without interruption. The advantage of this scheme is that the acceleration can be stepped up to a maximum without fear of the motor faltering owing to contactors dropping out.

It has been suggested that the scheme might be further simplified by the adoption of rectifier banks carrying the full-load current of the motor. These would be connected in the orthodox delta formation with the armature or field across the corners. This scheme has not yet been tried out, as scheme B was operating successfully, but it has definite possibilities.

Ingots are still dropped, but the modifications have reduced the extent of the damage sustained. The standard rail track has the advantage of that slight resilience which seems to withstand the shock better, and replacement of track is greatly simplified. The 'live' pick-up trolley wire lies exposed in a shallow concrete trench 12 in. wide by 9 in. deep, along which the collector from the chariot runs in contact with the wire. It has been found that the standard grooved trolley wheel gives the most satisfactory service; it is arranged so that the wheel lifts the wire from the wooden V-block as the chariot travels past and allows the wire to fall back naturally into position. This system gives maximum flexibility, and, even when the running rails are badly damaged and the chariot is running erratically, the wire allows for the excessive vertical and lateral movement of the collector.

An ingot is occasionally dropped directly on to the chariot itself, usually just as the tongs are about to position the ingot. The shock often lifts the wheels of the chariot from the track, which on the old scheme would have meant broken collectors, but the wire has sufficient flexibility to prevent this.

It has also been possible to improve the acceleration and general performance of the chariot, since the electrical pick-up system is no longer so dependent on the true running of the machine; the present time taken from No. 12 pit to the mill rack is 41 sec.

Fig. 4—Contactor assembly on chariot. Heavy white connections indicate series coil

ECONOMIC ADVANTAGES

It is impossible to give complete figures of saving in cost and labour, because there are so many variable factors. The most significant saving was shown in the labour expended at week-ends, when the all-night efforts of gangs of men used to be required to ensure that the track and chariot would be capable of running another week without an intermediate shutdown. It became possible to dispense with this concentrated effort. The relative costs of civil engineering for the six-monthly period before and after the alteration were calculated at £4343 and £1010 respectively. The mid-week delays due to mechanical and electrical causes, which show considerable improvement, take no account of the toil and discomfort previously experienced by the maintenance staff. This improvement was as follows:

	Electrical	Mechanical
6 *months before alteration*		
No. of incidents	82	71
Time lost, min.	419	374
6 *months after alteration*		
No. of incidents	36	33
Time lost, min.	182	156

Acknowledgments

The decision to allow these notes to be published, made by Mr. James Mitchell, Managing Director of Iron and Steel Production, was justified, since other plants are experiencing the same troubles, and it will give great satisfaction to the authors to know that they may have given assistance in this direction. It is also pleasing to acknowledge the teamwork between the Civil, Mechanical, and Electrical Engineering Departments, and the advice given by the Corby General Manager, without which the scheme would never have materialized.

A Thermal and Microscopic Study of the Iron–Carbon–Silicon–System

By J. E. Hilliard, B.Eng., Ph.D., and W. S. Owen, M.Eng., Ph.D.

SYNOPSIS

Constant-silicon sections of the metastable iron–carbon–silicon diagram at 2·3%, 3·5%, 5·2%, and 7·9% silicon have been determined by a thermal analysis and microscopic study of vacuum-melted high-purity alloys. These results have been confirmed and clarified by quenching experiments.

The invariant compositions of the ferrite, austenite, and melt phases (which define three of the corners of a four-phase isothermal plane) were found to be:

	Silicon, %	Carbon, %	Temp., °C.
Ferrite	8·25	0·4	1145
Austenite	7·20	0·6	,,
Melt	8·35	1·92	,,

This new configuration of the four-phase plane removes the restriction previously imposed on the position of the carbide corner, which can now be placed at the composition of Fe_3C, in agreement with recent experimental evidence.

Projections on two of the binary sides and four isothermal sections of the system are reproduced.

MUCH information on the Fe–C–Si system has been published since the pioneer work of Gontermann[1] in 1908, but there appears to be a general disagreement in the positions suggested for the cardinal points of the diagram and a lack of precise information concerning the changes in phase relationships during solidification. The work now described was undertaken to obtain more reliable experimental data on some of the outstanding anomalies. It is primarily concerned with changes in the liquidus–solidus regions of the system.

The composition plots of the alloys examined lie within the area of the diagram that is of fundamental importance to the subject of cast-iron metallography.

GENERAL FORM OF THE TERNARY DIAGRAM

Binary Systems

Within the relevant range of the iron–silicon diagram, the liquidus temperature is progressively lowered with increasing silicon, until, at about 20% silicon, the alpha–epsilon (FeSi) eutectic reaction occurs. In Fig. 1, the liquidus and solidus curves have been drawn from the results of Haughton and Becker,[2] supplemented by those of Ruer and Klesper[3] for the low-silicon region. The austenite phase is confined to a small area within the range 0–2·5% silicon by a 'gamma loop,' the boundary of which was established by Oberhoffer and Kreutzer.[4]

It is now generally accepted that graphite is the stable phase in iron–carbon alloys, cementite being a metastable phase of considerable persistence under some conditions. The superimposition of the stable and metastable diagrams gives the well-known double iron–carbon diagram.[5]

Development of the Binary Sides

As silicon is an element which is known to promote graphitization, it would be surprising if a region of carbide stability existed in the ternary system. The results of the present and previous investigations have therefore been interpreted on the basis of a 'double' ternary diagram, with graphite and carbide as the stable and metastable phases respectively.

In the iron-rich corner of the metastable diagram, four phases are known to occur: the melt, ferrite (alpha or delta solid solution), and carbide. Application of the phase rule shows that when the four phases coexist, the system is invariant, the compositions of the participating phases being defined by points lying on an isothermal plane. Each point lies at the intersection of three lines of three-fold saturation. There are 12 such lines forming the edges of four three-phase regions of triangular cross-section.

The type of reaction that takes place at an isothermal plane is characterized by the disposition of the four three-phase regions. The occurrence of both a peritectic and a eutectic reaction in the Fe–C system suggests that the four-phase reaction is of the type known as a peritecto-eutectic reaction, the four regions being divided equally, two above and two below the plane. With this arrangement, the four-phase quadrilateral plane cannot contain a re-entrant angle.

From theoretical considerations, Scheil[6] deduced the arrangement of the four-phase plane shown in Fig. 2. The points c, L, γ, and α represent the invariant compositions of the phases: carbide, melt,

Manuscript first received 6th September, 1951, and in its final form 19th May, 1952.

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ferrite, and austenite respectively. The two three-phase regions approaching the top of the plane are

(*a*) Melt + austenite + ferrite
(*b*) Melt + ferrite + carbide

Region (*a*) is a development of the peritectic reaction in the Fe–C system. Region (*b*) is a eutectic reaction which probably originates from a second four-phase plane lying somewhere near the Fe–Si side of the diagram. It cannot come directly from the Fe–Si side, as there is no reaction in that system which would account for the carbide phase.

The regions approaching the underside of the plane are

(*c*) Melt + austenite + carbide
(*d*) Ferrite + austenite + carbide

Both these regions develop from the Fe–C system: (*c*) from the eutectic, and (*d*) from the eutectoid reaction.

The disposition of the eutectic regions (*b*) and (*c*) implies that the temperature of the eutectic reaction rises with increasing silicon, and that the invariant temperature of the four-phase plane lies between the eutectic and peritectic temperatures of the Fe–C system.

The exact positions of the points α, γ, and c will be considered in detail later. However, it is clear from the arguments originally stated by Scheil that the positions shown in Fig. 2 cannot be sufficiently erroneous to affect the following discussion. The fourth corner L of the four-phase plane lies at the intersection of the two eutectic lines CL and LM with the peritectic line BL. From a practical point of view, this peritecto-eutectic point is of great importance, as it defines the composition of the melt when it is in equilibrium with the three solid phases. Scheil placed the L point between the corners c and γ on the edge of the plane nearest to the Fe–C side. Further consideration shows that this arrangement is not a unique solution of the problem, as it is possible for the L point to lie on the opposite side of the plane; *i.e.*, between the corners α and c. An examination of the alternative arrangement (Fig. 3*b*) shows that when the L point is moved to the other side of the plane, the order of the two eutectic three-phase regions, (*b*) melt + ferrite + carbide and (*c*) melt + austenite + carbide, is reversed, so that region (*b*) now approaches the underside of the plane and region (*c*) approaches the top of the plane. This requires the temperature of the eutectic to fall with increasing silicon content, and the temperature of the four-phase plane to lie between the eutectic and eutectoid temperatures of the Fe–C system.

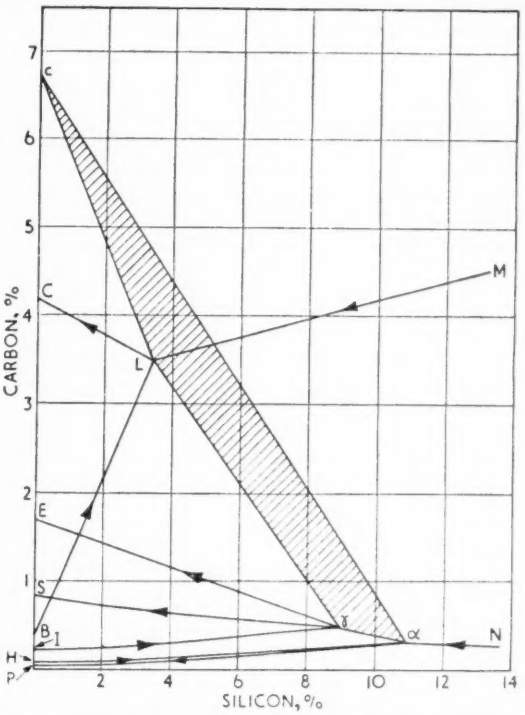


Fig. 2—Basal projection of Fe–C–Si diagram (Scheil,[6] 1930)

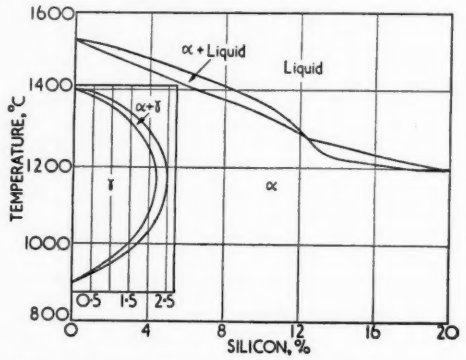


Fig. 1—The iron–silicon diagram between 0 and 20% silicon, from the data of Haughton and Becker,[2] Ruer and Klesper,[3] and Oberhoffer and Kreutzer[4]

The orientation of the trace made by the peritectic region on a vertical section parallel to the Fe–C side will also differ for the two arrangements of the four-phase plane. Considering, for example, a constant silicon section at AA (Fig. 3*a*), this section cuts the lines of three-fold saturation BL, $J\gamma$, and $H\alpha$ at the points b, j, and h. The total temperature drop along the lines BL, $J\gamma$, and $H\alpha$ is equal, as the three points B, J, and H are on an isothermal line and the points L, γ, and α are on an isothermal plane. Assuming, as a first approximation, that the points B and L, J and γ, H and α are connected by straight lines, then the ratios of the intercepts Bb, Jj, and Hh to the total length of the lines are proportional to the temperature intervals B to b, J to j, and H to h. It will be seen that

$$\frac{Bb}{BL} > \frac{Jj}{J\gamma} > \frac{Hh}{H\alpha}$$

Hence b, j, and h are at progressively higher temperatures and, consequently, the peritectic three-phase region slopes downwards. The complete constant-silicon section at AA is shown in Fig. 3*c*.

By a similar analysis of the section $A'A'$ for the

alternative position of the L point shown in Fig. 3*b*, it can be shown that

$$\frac{J'j'}{J'\gamma'} > \frac{H'h'}{H'\alpha'} > \frac{B'b'}{B'L'}$$

The points in order of increasing temperature are therefore j', h', and b'. The corresponding constant-silicon section is given in Fig. 3*d*, from which it will be observed that the peritectic region slopes upwards. The slope of the eutectic field remains unchanged, as the effect of the shift in the position of point L is

The austenite composition surfaces of these regions intersect at the lines of three-fold saturation, $E\gamma$, $S\gamma$, and $J\gamma$, forming a triangular pyramid, the apex of which lies at the corner γ of the four-phase plane. The $\alpha + \gamma$ two-phase region, which covers the side of the gamma region nearest to the Fe–Si side of the system, terminates at the point α.

The most noticeable difference between the diagrams for the stable and metastable systems is in the position of the carbon-rich corner of the four-phase plane. In the stable system this corner lies at 100% carbon, so

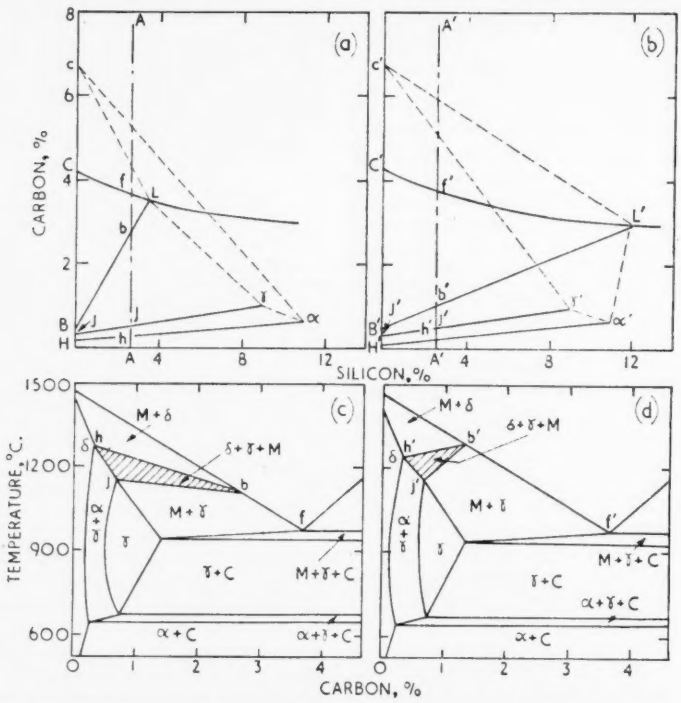


Fig. 3—The two possible arrangements of the four-phase plane and the corresponding constant-silicon sections: (*a*) Basal projection of the four-phase plane according to Scheil[6]; (*b*) basal projection showing alternative arrangement of the four-phase plane; (*c*) constant-silicon section on AA (schematic); and (*d*) constant-silicon section on A′A′ (schematic)

counterbalanced by the reversal of the three-phase region: melt + austenite + carbide.

The remaining features of Scheil's diagram need not be considered in detail. The liquidus surface can be visualized as a triangular sheet pegged to the Fe–C liquidus line on the one side and to the Fe–Si liquidus on the other, with a 'fold' corresponding to the eutectic line CLM and a 'crease' corresponding to the peritectic line BL (Fig. 2). From C to L the melt is in equilibrium with austenite and carbide and from L to M with ferrite and carbide. Along the peritectic line BL, the melt is in equilibrium with austenite and ferrite. With increasing silicon, this line moves downwards and away from the Fe–Si side until it intersects the eutectic curve at the peritecto-eutectic point.

The gamma-phase region is bounded by the three two-phase regions: γ + melt, γ + carbide, and $\gamma + \alpha$.

that when the diagram is plotted on rectangular axes, the lines joining it to the α and γ corners will be parallel to the Fe–C side of the diagram.

Several forms of the diagram which are radically different from that suggested by Scheil have been proposed. The most important are those due to Hanson[7] and to Jänecke.[8] However, they all assume, or imply, the existence of a temperature range in which the carbide is the stable phase, and they will not therefore be discussed here.

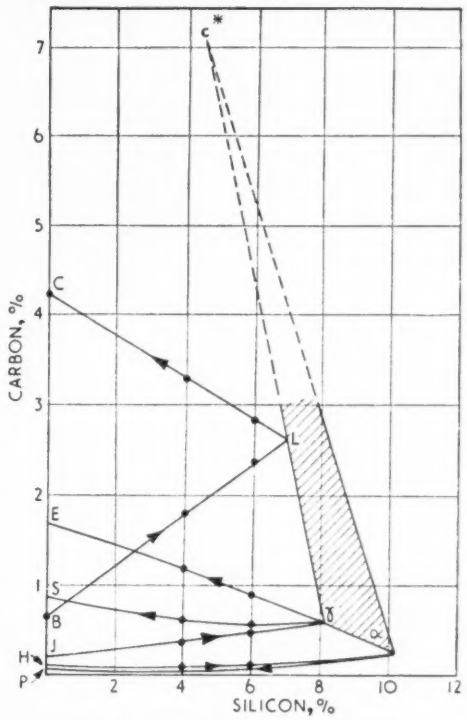
PREVIOUS EXPERIMENTAL WORK

The quantitative diagram that is now generally accepted as being the most reliable is based upon the results of Kříž and Pobořil,[9, 10] Hanemann and Jass,[11, 12] and Satô.[13] Of these investigations, that by Kříž and Pobořil was the most extensive. Their

results for the metastable system are shown in the basal projection, Fig. 4. It will be seen that the *L* point was placed on the low-silicon side of the γ point, the diagram being qualitatively the same as Scheil's.[6] Having determined the positions of the α, γ, and *L* corners of the four-phase plane in the metastable system, it was necessary to place the carbide corner (point *c*, which was not determined experimentally) at about 4·5% silicon, to comply with the condition that the quadrilateral cannot contain a re-entrant angle. Reviewing this work, Scheil[14] suggested that the available data were insufficient to accept this large solubility of silicon in a carbide based upon Fe_3C.

The accuracy achieved by Kříž and Pobořil was not of the standard now generally expected in work of this nature. The alloys were prepared from low-carbon iron, Swedish pig iron, ferro-silicon, and a 25% silicon intermediate alloy; thus, they were very impure. Some of them contained as much as 0·5% of manganese, as well as appreciable quantities of other elements. No correction was made for the appreciable difference between the nominal and actual compositions of many of the alloys. The thermal-analysis ingots were cooled at a rate of 30° C./min.: subsequent work has shown that this rate is too rapid for reasonable accuracy to be achieved. Under these conditions, alloys containing less than 6% of silicon solidified white, and excessive extrapolation was necessary to complete the projections of the stable and metastable systems.

Hanemann and Jass[11, 12] determined the path of the eutectic line. A series of slightly hyper-eutectic alloys ranging from 0–22% of silicon was prepared by melting mixtures of Armco iron, ferro-silicon, and sugar carbon in graphite crucibles. A sample from each ingot was then remelted under vacuum in a H.F. furnace, and slowly cooled. After the removal of the hyper-eutectic (kish) graphite, the operation was repeated until no further separation occurred. The resultant ingots, which were assumed to be of eutectic composition, were analysed for carbon and silicon, thus enabling the composition variation of the eutectic to be plotted. The liquidus and solidus temperatures of samples from each ingot were determined by thermal analysis, the cooling rate being about 5° C./min. All the alloys—with the exception



* Point *c* was not determined experimentally

Fig. 4—Basal projection of the metastable Fe–C–Si diagram, from the results of Kříž and Pobořil[9, 10]

of the first of the series, which contained only 0·03% of silicon—solidified with the formation of graphite. However, the deduction that the specimens solidified with the formation of the stable austenite–graphite eutectic may be questioned, as some of the specimens had a pronounced dendritic structure.

Hanemann and Jass's results for the course of the eutectic curve are included in Figs. 5*a* and *b*. On the basis of a supposed discontinuity in the curve, they placed the peritecto-eutectic point at 6% silicon and 2·5% carbon. However, their close agreement with

Table I

POSITIONS OF THE CORNERS OF THE FOUR-PHASE PLANE

Investigators	Composition, %	Metastable System				Stable System			
		α	γ	c	L	α	γ	c	L
Kříž and Pobořil	Si	10·2	8·2	4·0*	6·9	9·7	7·7	0	6·4
	C	0·26	0·54	7·0*	2·61	0·22	0·52	100	2·54
Satô	Si	12·0	9·0	...	...	11·3	6·7	...	...
	C	0·39	0·39	...	...	0·31	0·31	...	...
Hanemann and Jass	Si	...	...	...	...	...	...	...	6·0
	C	...	...	...	...	...	...	...	2·5
Present investigation	Si	8·25	7·2	0	8·35	...	...	...	...
	C	0·4	0·6	6·7	1·92	...	...	...	...

*Approximate limiting position

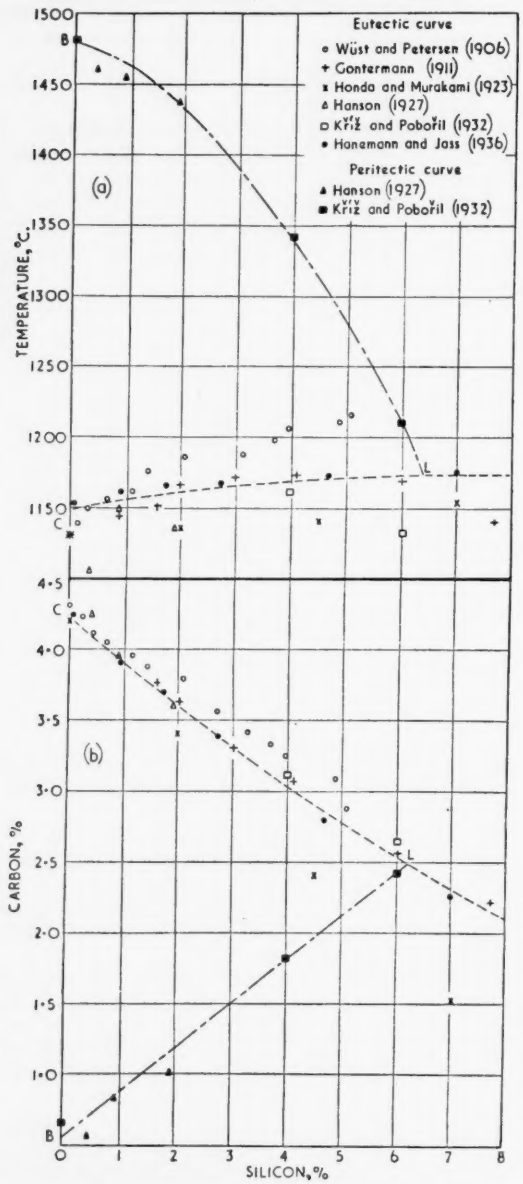


Fig. 5—Peritectic and eutectic melt lines of two-fold saturation, showing: (*a*) Temperature variation, and (*b*) composition variation

Kříž and Pobořil on the position of this point must be considered as being somewhat fortuitous, as curves showing an intersection of anywhere between 5 and 8% of silicon can be drawn equally as well through their experimental points.

By dilatometric and microscopic methods, Satô[13] examined alloys in the range 0–8% silicon and 0–1·6% carbon, and traced the lines $E\gamma$, $S\gamma$, $H\alpha$, and $P\alpha$. The positions (Table I) given by Satô to the α and γ corners of the four-phase plane are probably the most reliable so far obtained. Appreciable extrapolation was necessary, however, for the location of the α corner, and its position is therefore uncertain.

In addition to the three that have been mentioned, descriptions of other investigations of the system have been published, but these will not be considered in detail, as the results were expressed in terms of diagrams which now appear to be unacceptable. The only work in which high-purity materials and very accurate experimental methods were employed was that of Hanson.[7] Unfortunately, the results reported are restricted to the range of 0–1·9% silicon. Experimental points taken from the work of Wüst and Petersen,[15] Gontermann,[1] Honda and Murakami,[16] Hanson,[7] Kříž and Pobořil,[9, 10] and Hanemann and Jass[11, 12] are shown in Figs. 5*a* and *b*. The agreement between the results for the path of the composition variation of the eutectic is surprisingly good, although the results for the temperature variation show a larger scatter. It would appear, however, that the temperature of the eutectic rises with increasing silicon. The data defining the peritectic curve are scanty. The results of Hanson are in fair agreement with those obtained by Kříž and Pobořil.

The problem of the composition of the carbon-rich phase in the metastable system has been reviewed recently by Owen,[17] who suggests that the phase is Fe_3C, of negligible silicon content. This view is supported by the chemical work of Hultgren and Kuo.[18] The nature of the graphite phase in Fe–C–Si alloys has been discussed by Owen and Street[19] and by Taylor.[20] There is general agreement that its composition should be represented by the point 100% carbon.

EXPERIMENTAL PROCEDURE

The principal aim of the present work was to redetermine the position of the peritecto-eutectic point. As this point lies at the intersection of the peritectic and eutectic curves, it has been necessary to trace the temperature and composition variation of these reactions. This has been done by determining four constant-silicon sections at 2·3%, 3·5%, 5·2%, and 7·9% silicon, using data obtained by thermal analysis supplemented by the metallographic examination of specimens quenched from within the liquidus–solidus range.

The constituent elements used in the investigation were of the highest purity available. Analyses of the silicon and Swedish iron are given in Tables II and III. Carbon was added in the form of graphite, the ash content of which was less than 0·07%. All the melting was carried out in recrystallized alumina crucibles. Both Adcock[21] and Hanson[7] have reported contamination of iron alloys due to reduction of alumina by carbon or silicon at high temperatures. However, the spectrographic analysis of a representative batch of ingots obtained during the course of the present work failed to reveal the presence of aluminium.

Preparation of Alloys

90-g. ingots of each alloy were prepared in a H.F. vacuum melting furnace, the charge consisting of Swedish iron, graphite in the form of pellets, and a coarsely crushed 50% silicon master alloy. The alloy

was kept molten at a pressure of 10^{-4} mm. Hg. for about 30 min., to ensure thorough mixing and outgassing. When cool, the ingots came cleanly away from the recrystallized alumina crucibles, and no superficial oxidation could be detected.

Each ingot was sectioned longitudinally, and the face of one half was prepared and examined microscopically. Segregation was detected in two ingots, which were remelted.

Thermal Analysis

The thermal analysis was carried out in an electrical resistance furnace wound with 10% rhodium–platinum wire, backed by a 'booster' winding of Kanthal A-1 strip. The power input to the furnace was controlled automatically to produce linear heating and cooling rates, a refinement which greatly facilitated the interpretation of the thermal curves. The ingot and crucible were contained in a mullite closed-end tube, which was continuously evacuated by a mercury diffusion pump backed by a rotary oil pump. The mullite tube remained substantially vacuum-tight up to the highest temperatures employed; even at 1550° C., a pressure as low as 5×10^{-3} mm. Hg. could still be maintained.

The temperature of the alloy was measured by means of a Pt/Pt–10% Rh thermocouple made from calibrated wire. Each thermocouple was used about three times and, between runs, a length of 6 in. was removed from the hot junction end. The thermocouple was protected by a recrystallized alumina sheath, 20 cm. long × 4 mm. O.D. and 2 mm. I.D. The thermal e.m.f. was measured with a null-deflection precision potentiometer having an accuracy of about 1 μV. (corresponding to about 0·1° C.).

The inverse-rate method of thermal analysis was employed, and the time intervals, corresponding to a change of 20 μV. of the thermocouple e.m.f. (*i.e.*, about 2° C.), were recorded on a tape chronograph. This instrument was so designed that the curves could be plotted directly from the tape. During thermal analysis, the temperature of the furnace was changed at a linear rate of about 4° C./min.

The reproducibility of the thermal curves was good, the variation in the observed arrest temperatures for any particular alloy seldom being greater than 3° C. A test on a sample of commercially 'pure' nickel gave a temperature for the freezing point of the metal which was about 6° C. lower than the accepted value (1445° C.). However, this depression of the freezing point may have been partly due to the presence of impurities.

After thermal analysis, the ingots were sectioned longitudinally, one half being used for microscopical examination and the other to provide the samples required for chemical analysis and for the quenching experiments.

Quenching Experiments

To supplement the thermal data, specimens from certain of the ingots were quenched from various temperatures within the liquidus–solidus range. The quenching furnace consisted essentially of a vertical 1-in. dia. mullite tube wound with tungsten wire. The bottom of the tube was sealed by a steel plate held in position by two electromagnets, and the top was sealed by a brass plug through which passed two electrodes and connections for the thermocouple. The specimen, in the form of a 0·5-cm. cube, rested in a small crucible, which was suspended by platinum wire from a loop of fuse wire stretched across the two electrodes in the plug. The furnace was brought to within a few degrees of the required temperature, and the specimen assembly was quickly lowered into the mullite tube. The furnace was immediately evacuated, and then filled with argon, which was maintained at a slight positive pressure. The specimen was held at the desired temperature for 10–30 min., and then quenched by depressing a switch which diverted the current from the electromagnets to the fuse wire, thus allowing the specimen to fall through the tube into a bath of brine solution. The annealing temperature was measured by an insulated but unsheathed thermocouple, whose hot junction was suspended within 2 mm. of the specimen.

After quenching, each specimen was tempered for 15 sec. in a molten lead–tin bath held at 350° C., to improve the etching characteristics of the decomposed austenite products.

A fuller account of this and the other apparatus has been given elsewhere.[22]

Table II

SILICON ANALYSIS

	%
Silicon	99·80
Iron	0·006
Aluminium	0·022
Calcium	0·006
Titanium plus zirconium	0·003
Phosphorus	0·005
Carbon	0·01
Manganese	Nil
Copper	,,
Cobalt	,,
Nickel	,,

Table III

SWEDISH IRON ANALYSIS

	%
Carbon	0·018
Silicon	0·013
Sulphur	0·006
Phosphorus	0·001
Manganese	0·007
Nickel	0·0037
Chromium	0·006
Copper	0·006
Oxygen	0·044
Nitrogen	0·01
Hydrogen	Trace
Total impurities other than C, Si, and gases	0·029%

EXPERIMENTAL RESULTS

Thermal Analysis Results

The compositions of the 24 alloys examined and the thermal analysis results are set out in Table IV. When more than one cooling (or heating) curve was taken through a particular transformation, the following information was tabulated: (i) the mean temperature of the arrest, (ii) the number of deter-

Table IV

THERMAL ANALYSIS RESULTS

Alloy	Composition		Liquidus						Peritectic						Eutectic						Solidus			Eutectoid			Corrected Temperatures			
			Cooling			Heating			Cooling			Heating			Cooling			Heating			Heating			Cooling			Liquidus		Peritectic	
	Silicon, %	Carbon, %	Temp., °C.	N	D	Temp., °C.	N	D	Temp., °C.	N	D	Temp., °C.	N	D	Temp., °C.	N	D	Temp., °C.	N	D	Temp., °C.	N	D	Temp., °C.	N	D	Cooling	Heating	Cooling	Heating
A1	2·38	0·16	1482	2	0	n.d.	...	...	1416	5	2	1421	3	2										741	1	...			1419	1424
									1409	5	5	1433	3	2	...	...	...	...	...	...	1343	1	...	726	1	...	1483	n.d.	1412	1436
									1434	2	1	1432	1	...															1431	1429
A2	2·20	0·38	1469	2	1	1471	1	...	1415	2	2	1444	1	...	...	...	...	...	...	...	1402	1	...	n.d.	...	...	1467	1469	1412	1441
									1428	3	3	...	...	...										731	1	...				
A3	2·29	0·53	1454	4	2	1460	1	..	1414	3	5	1444	1	...	...	...	...	...	...	...	1377	2	6	702	1	...	...	...	...	...
															...	...	...	...	...	...				741	1	...	...	...	...	...
A4	2·28	0·98	1416	2	1	1425	1	...	...	...	...	...	...	...	...	...	...	...	...	...	1279	1	...	720	1	...	...	...	...	...
															1146	2	1	1149	1	...				761	1	...				
A5	2·22	1·93	1333	2	1	1336	1	...	...	...	...	...	...	...	1134	2	2	1161	1	...	...	...	...	741	1	...	1332	1335	..	..
															1161	3	1	1150	2	1	...	...	...	n.d.	...	...				
A6	2·15	3·67	1161	3	1	1150	...	...	...	...	...	...	...	...	1134	3	5	1164	1	...	...	...	...	n.d.	...	...	1161	1150	...	...
B1	3·42	0·47	1441	2	1	n.d.	...	...	1399	2	0	1397	1	...										763	1	...			1397	1395
									1383	2	1	1407	1	...	..	...	...	...	...	...	1345	1	...	748	1	...	1440	n.d.	1381	1405
									1403	2	1	1403	2	2															1398	1398
B2	3·28	0·51	1443	2	1	1443	2	1	1385	2	1	1414	2	2	...	...	...	...	...	...	1345	1	...	n.d.	...	...	1440	1440	1380	1409
									1402	3	2																			
B3	3·54	0·85	1414	1	...	...	...	...	1397	3	10	1404	2	1	...	...	...	...	...	...	1264	2	10	n.d.	...	...	...	...	...	...
															...	...	...	...	...	...				792	1	...				
B4	3·49	1·06	1396	2	0	1396	2	5	...	...	...	...	...	...	...	...	...	...	...	...	1231	1	...	752	1	...	...	...	...	...
															1132	2	1	1162	2	2				771	1	...				
B5	3·43	1·52	1359	2	0	1357	2	2	...	...	...	...	...	...	1121	2	1	1174	2	2	...	...	...	n.d.	...	...	1358	1356	...	...
															1141	2	1	1170	1	...				796	1	...				
B6	3·46	2·06	1282*	2	1	1315	1	...	...	...	...	...	...	...	1125	2	1	1206	1	...	...	...	...	n.d.	...	...	...	...	...	...
															1160	2	1	1163	2	1				810	1	...				
B7	3·45	3·07	1187	2	1	1196	1		...	...	...	...	...	...	1148	2	2	1177	2	1	...	...	...	794	1	...	...	...	...	...
															1165	3	1	1164	2	1				808	1	...				
B8	3·40	3·35	1165	3	1	1164	2	1	...	...	...	...	...	...	1146	3	1	1173	2	1	...	...	...	798	1	...	1165	1164	...	...
									1312	2	0																		1326	...
C1	5·55	0·16	1446	2	3	1444	1		1304	2	0	...	...	...	...	...	...	...	...	...	...	...	...	n.d.	...	...	1449	1447	1318	...
									1330	4	4	1328	3	2										801	1					
C2	5·23	0·54	1409	4	2	1411	3	2	1320	4	2	1348	3	4	...	...	...	...	...	...	1279	2	5	787	1	...	...	...	...	...
									1342	3	0	1346	2	2															1340	1344
C3	5·15	0·75	1385	3	1	n.d.	...	...	1320	3	0	1358	2	0	...	...	...	...	...	...	1238	1	...	n.d.	...	...	1384	n.d.	1318	1356
									1336	2	1																		1333	...
C4	5·12	1·02	1353	2	3	1350	2	3	1326	2	2	...	...	...	...	...	...	...	...	...	1154	1	...	n.d.	...	...	1350	1347	1323	...
															1153	2	1	1160	1	...				887	1	...				
C5	5·12	2·02	1251	1	...	...	...	...	...	...	...	...	...	...	1143	2	1	1167	1	...	...	...	...	878	1	...	1248	...	...	...
															1165	2	0	1163	1	...				888	1	...				
C6	5·14	2·84	1165	2	0	1163	1	...	...	...	...	...	...	...	1147	2	2	1172	1	...	...	...	...	880	1	...	...	...	...	...
															...	...	...	...	...	...										
D1	8·10†	0·46	1352	3	3	1352	2	2	1188	2	0	...	...	...	...	...	...	...	...	...	1163	1	...	n.d.	...	...	1358	1358	1198	...
															1129	2	0	1146	1	...										
D2	7·76†	1·00	1280	2	2	1287	1	...	1184	2	0	1213	1	...	1104	2	0	1159	1	...	...	...	...	n.d.	...	...	1276	1283	1176	1205
															1141	3	2	1149	2	1										
D3	7·86	1·46	1227	3	4	1240	2	2	1188	3	3	1211	1	...	1121	3	4	1164	2	1	...	...	...	n.d.	...	...	...	...	...	...
															1152	2	0	1148	2	0										
D4	7·92	2·33	1152	2	0	1163	2	0	...	...	...	...	...	...	1125	2	2	1163	2	0	...	...	...	n.d.	...	...	...	...	...	...

* Temperature a little low, due to misalignment of thermocouple sheath
† Uncorrected temperatures used in plot of 7·9% silicon section
N = Number of determinations
D = Maximum deviation from mean in degrees centigrade
n.d. = Not determined

XUM

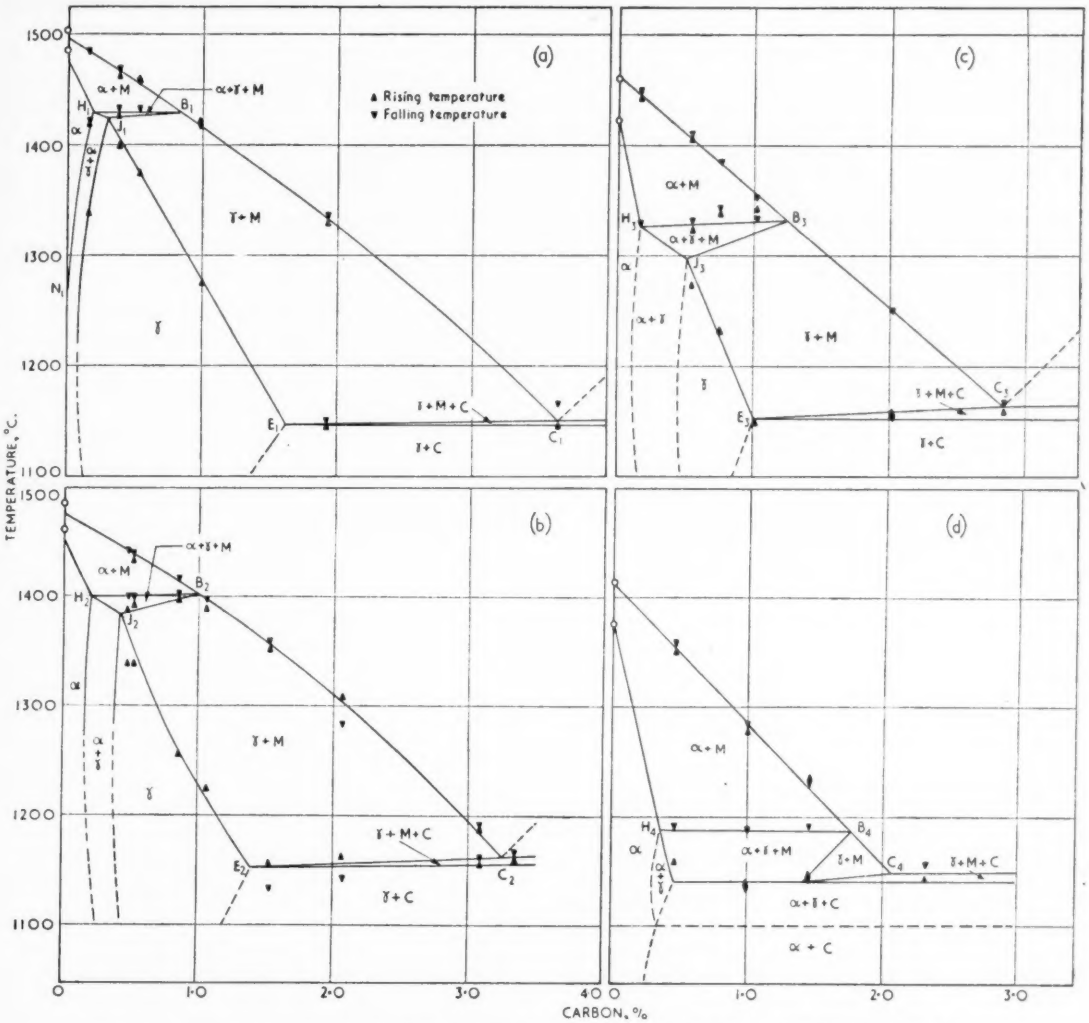


Fig. 6—Thermal arrests: Vertical silicon sections at (*a*) 2·3%, (*b*) 3·5%, (*c*) 5·2%, and (*d*) 7·9%

minations made, and (iii) the maximum deviation observed from the mean temperature. For the peritectic and eutectic transformations, the temperature is also given for the point at which the inverse-rate curve returns to the datum line after the arrest. Although this point has no fundamental significance,[23] it does give a relative indication of the interval of the arrest, because the rate of cooling (or heating) and the thermal capacity of the system were kept sensibly constant.

Corrected liquidus and peritectic temperatures (estimated by interpolation between sections) are given for those alloys whose silicon content differed from the nominal value for the section by more than 1·5% (the estimated accuracy of the chemical analysis). No correction was necessary for the eutectic arrests, because of the small temperature variation of this transformation.

The corrected values for the thermal arrests are plotted on the constant-silicon sections in Figs. 6*a*, *b*, *c*, and *d*.

The liquidus temperatures lie close to smooth curves which, when extrapolated, intersect the binary iron–silicon diagram at temperatures that differ from those determined by Haughton and Becker[2] by less than 5° C.

The four alloys that were very nearly of eutectic composition showed ' double-arrests ' on the cooling curves at the eutectic temperature. The second arrest, about 4° C. below the first, was the longer of the two. Because of the small temperature difference involved, this double arrest was only detected when an e.m.f. interval of 10 μV. was used.

The temperatures of the peritectic arrest in the 2·3% silicon alloys (A2 and A3) containing 0·38% and 0·53% of carbon differ by only one degree, whereas the arrest observed immediately below the liquidus for 0·16% carbon alloy A1 was 15° C. lower and also

exhibited slight undercooling. These facts suggest that the latter arrest was due to the start of the ferrite → austenite transformation, and not to the peritectic reaction. Consequently, the boundary line N_1H_1 (Fig. 6*a*) separating the α and $(\alpha + \gamma)$ regions has been drawn to intersect the peritectic line H_1B_1 on the high-carbon side of the plot of the 0·16% carbon alloy.

Microstructures of the Thermal Analysis Alloys

Seventeen of the alloys had microstructures which were uniform over the whole transverse section of the ingot. The structures were in agreement with those predicted by consideration of the vertical sections.

The eutectic graphite in the hypo-eutectic alloys appeared as 'undercooled' (A.S.T.M. classification: Type D) graphite. Following the work of Eash,[24] and Morrogh and Williams[25] it has been shown by Owen and Street[26] that, in vacuum-melted pure alloys, this structure forms as a result of the decomposition of an undercooled austenite–cementite eutectic. Thus, the thermal analysis data obtained from these alloys refer to the metastable iron–cementite system.

The four 'eutectic' alloys that showed anomalous double arrests in cooling had unusual macrostructures. Initially, these alloys were hyper-eutectic in composition. During melting in the H.F. furnace, pro-eutectic (kish) graphite was precipitated and subsequently removed. In spite of this treatment, the compositions were still slightly hyper-eutectic after remelting in the thermal analysis furnace, a small quantity of kish graphite being found near the top of each ingot (Fig. 8*b*).

The macrostructures of the 'eutectic' alloys A6 and B8 were similar to that of C6 illustrated in Fig. 7. The volume surrounding the thermocouple sheath had a eutectic structure (Fig. 8*a*), in which the graphite flakes were larger than those normally associated with an undercooled structure. It is therefore suspected that these alloys solidified in the stable system: this was presumably due to the inoculating effect of the kish graphite. In the immediate vicinity of the crucible walls, the structure was also eutectic, with a tendency for the graphite flakes to be orientated at right-angles to the surface. The most unusual feature of these ingots, however, was the occurrence of primary austenite dendrites in the annular zone between the central and outside areas.

The anomalous macrostructures and thermal arrests of these ingots can be explained in terms of silicon segregation resulting from thermal gradients. During cooling, the ingot loses heat from the centre by conduction along the thermocouple sheath and by radiation from the surfaces. Consequently, at any particular stage during cooling, there will be an annular zone, concentric with the thermocouple sheath, which is at a higher temperature than the surrounding metal. At the start of the eutectic transformation, the solid will advance simultaneously from the centre and from the walls of the crucible, the last of the melt to solidify being located in the annular zone. The eutectic transformation takes place over a range of temperature, and is accompanied by a change in composition of the solid and melt phases. If equilibrium is to be maintained, silicon must diffuse within the austenite and between the austenite and liquid interface. Owing to the unusually small temperature variation of the eutectic reaction in the Fe–C–Si system, the difference in silicon content of the melt and the first-formed austenite is particularly large. It is therefore not unreasonable to suppose that, despite their fairly slow cooling, the three 'eutectic' alloys departed from the equilibrium condition during solidification. Under these conditions, the last liquid to solidify would be depleted in silicon, and consequently hypo-eutectic, thus accounting for the formation of primary austenite dendrites in the annular zone.

There was no hyper-eutectic zone in the 'eutectic' alloy of the 7·9% silicon section, although there was a considerable difference between the size of the graphite flakes occurring in different parts of the ingot. Owing to the greater viscosity of the melt, rather more kish graphite was present than in the other 'eutectic' ingots.

Two low-carbon alloys of the 5·2% silicon section and one of the 7·9% section also showed effects due to segregation. (Micrographs of these alloys have been reproduced elsewhere in a paper by Owen and Street.[27*])

Alloy C3 had a structure [Fig. 5 (ref. 27)] consisting of about equal proportions of pearlite (which had a dendritic form) and silico-ferrite, in which were embedded needles and irregular-shaped particles of an iron–silicon carbide (the 'X-constituent'). In alloy C4 [Fig. 7*a* (ref. 27)] the dendritic character of the pearlite areas was more pronounced. The interdendritic material was again silico-ferrite plus the X-constituent. In addition, there were numerous pro-eutectoid carbide needles that had a duplex structure consisting of precipitated cementite in a matrix of the X-constituent [Fig. 7*b* (ref. 27)].

Although both specimens contained extensive ferrite areas, their compositions indicate that they should have a hyper-eutectoid structure. To explain this anomaly, it is again necessary to assume that segregation of silicon occurred during the formation of the austenite solid solution. As the rate of diffusion of silicon is very much less than that of carbon, silicon diffusion will be the controlling factor in the approach to equilibrium during crystallization of austenite from the melt. In the alloys under discussion, it is probable that, at the start of solidification, the temperature is sufficiently high to allow equilibrium to be maintained. As the temperature falls, there will be a rapid decrease in the diffusion rate until a stage is reached when there is incomplete transference of silicon from the freshly formed austenite to the existing areas. A 'cored' structure is thus formed in which the centre areas of the austenite dendrites will have a much lower silicon content than the last liquid to solidify.

As this liquid disappears principally by the diffusion of its components to the existing solid, the last-formed

* Where figure numbers are followed by '(ref. 27),' reference should be made to the figures in the paper by Owen and Street. It should be noted that alloys designated C3, C4, and D1 in the present paper correspond to numbers B38, B22, and B41, respectively, in the paper by Owen and Street.

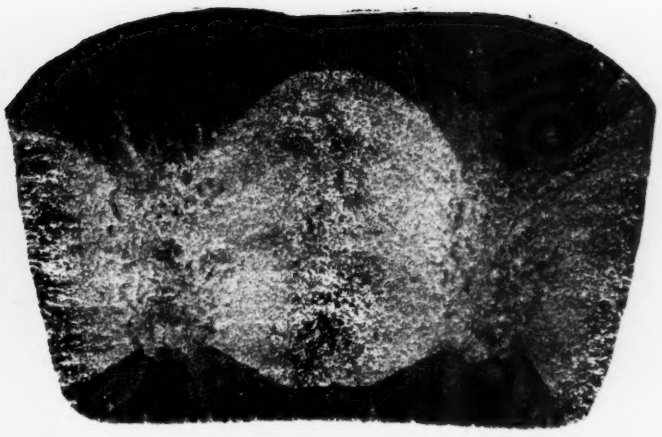
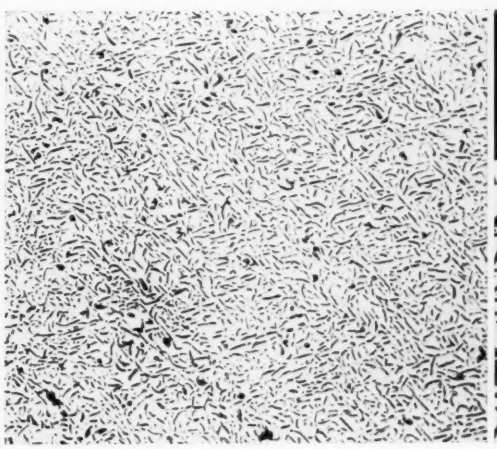


Fig. 7—Transverse section of ingot C6 (' eutectic ' alloy, 5·2% Si). Unetched, oblique lighting × 4

(a)

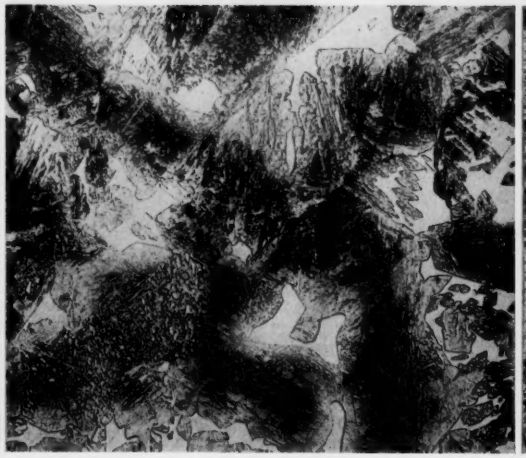


(b)

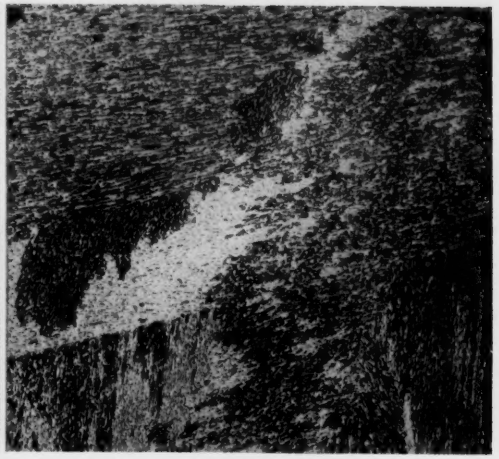
Fig. 8—Ingot C6 (' eutectic ' alloy, 5·2% Si), showing : (*a*) Fine flake graphite structure near the centre, and (*b*) kish graphite at the top. Unetched × 100

[*Hilliard and Owen*
[*To face p.* 276

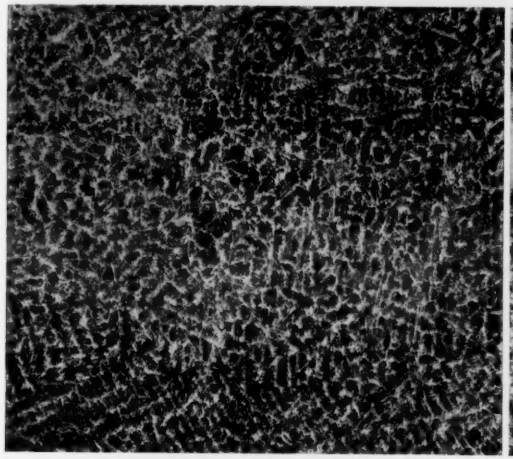
(*a*) A1. Quenching temperature 1475° C. Ferrite (white) plus transformed melt



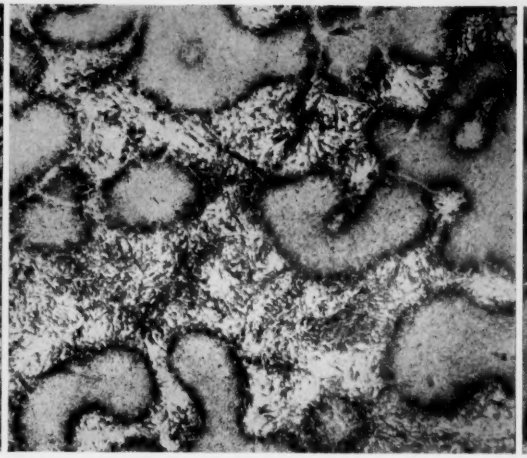
(*b*) A1. Quenching temperature 1425° C. Ferrite plus transformed austenite



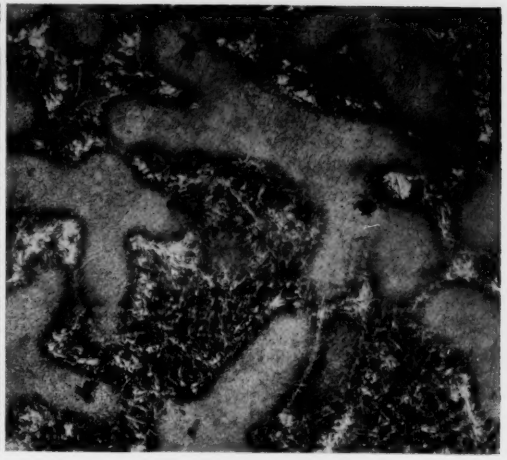
(*c*) A1. Quenching temperature 1295° C. Structure of specimen quenched from single-phase austenite region



(*d*) A4. Quenching temperature 1435° C. Martensite plus retained austenite structure of transformed melt



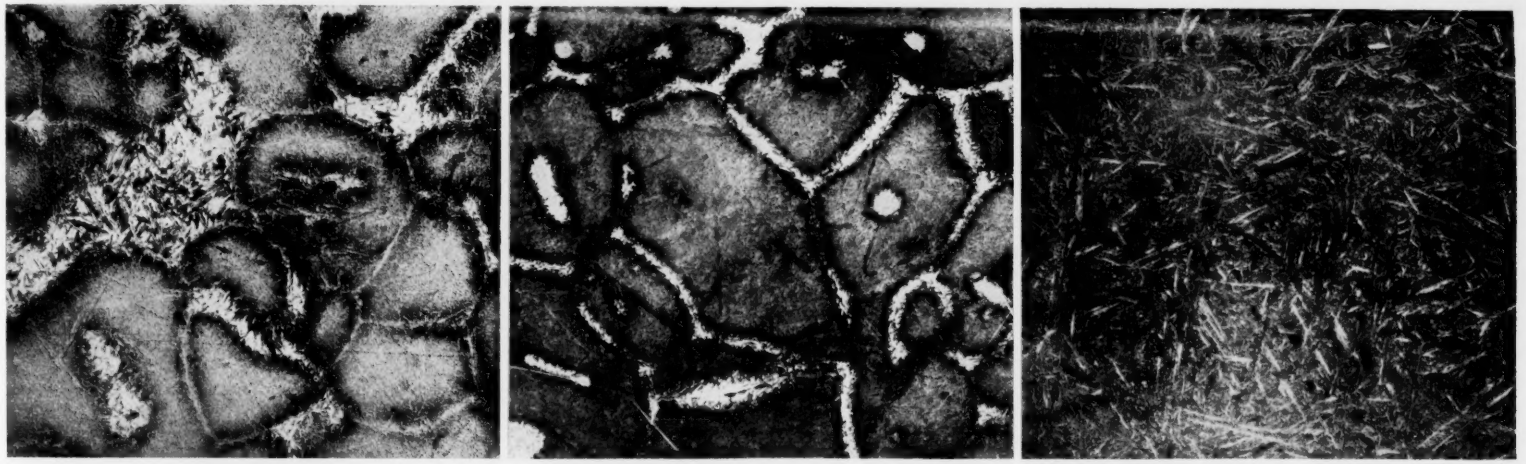
(*e*) A4. Quenching temperature 1415° C. Dendrites of transformed austenite surrounded by transformed melt



(*f*) A4. Quenching temperature 1405° C. Similar to (*e*).

Fig. 9 *a-f*—Quenched specimens from ingots A1 and A4. All specimens tempered at 320° C. for 15 sec. and etched with modified nital reagent × 100

Fig. 9 *a-f*—Quenched specimens from ingots A1 and A4. All specimens tempered at 320° C. for 15 sec. and etched with modified nital reagent × 100

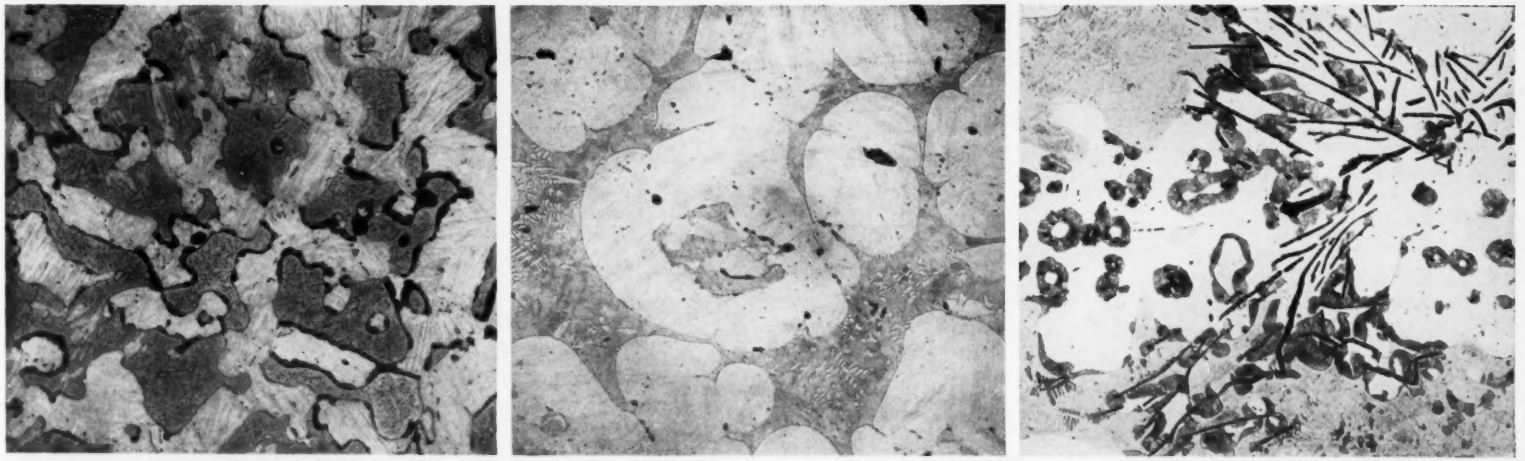


(*g*) A4. Quenching temperature 1395° C. Proportion of retained austenite in transformed melt areas increased

(*h*) A4. Quenching temperature 1345° C. Melt confined to a band around original austenite grains

(*i*) A4. Quenching temperature 1265° C. Quenched from single-phase austenite region. Dark-etched tempered martensite structure

Fig. 9 *g-i*—Quenched specimens from ingots A1 and A4. All specimens tempered at 320° C. for 15 sec. and etched with modified nital reagent × 100



(*a*) Alloy D1 (0·46% C)

(*b*) Alloy D2 (1·00% C)

(*c*) Alloy D3 (1·46% C)

Fig. 10—Specimens from alloys of the 7·9% Si group quenched from 1150° C. Tempered at 320° C. for 15 sec. and etched with modified nital reagent × 100

austenite will be even further enriched with silicon. Evidence for the formation of a cored structure is given by the micrograph [Fig. 5 (ref. 27)] of alloy C3 at low magnification. It will be seen that the pearlite areas (which correspond to the original austenite dendrites) have not etched uniformly, the outer edges being more resistant to attack than the centre regions. On cooling, the silicon-rich areas will decompose at a higher temperature than the remaining austenite, because the temperature of the eutectoid reaction increases with increasing silicon. It might at first be thought that this silicon-rich austenite would still decompose to give a typical pearlitic structure. This is not necessarily the case, however, as can be shown by considering alloy D1 of the 7·9% section. From the quenching data, it is known that, shortly after solidification, this alloy contained about equal proportions of austenite and ferrite. In the slowly cooled ingot, however, there was no distinction between the original phase areas, the structure consisting of needles of the X-constituent in a silico-ferrite matrix [Figs. 8*a* and *b* (ref. 27)], closely resembling the interdendritic areas of alloys C3 and C4. It is therefore feasible that the structures of these two alloys can be explained on the basis of the formation of a silicon-enriched austenite that decomposes to give ferrite and the X-constituent.

Alloy D1 also provides direct metallographic evidence for the segregation of silicon. As will be seen from Fig. 8*b* (ref. 27), the silico-ferrite matrix etched heterogeneously, and it is reasonable to suppose that the silicon content of those areas that showed little attack is higher than in the remaining portions.

Although it is necessary to postulate a departure from equilibrium for alloys C2, C3, D1, and the three eutectic alloys, this in no way invalidates the thermal data, as the segregation occurred below the lowest recorded transformation temperature. It is apparent, however, that the reliability of earlier thermal determinations of transformations in the solid state must be seriously questioned, unless abnormally slow rates of cooling were employed.

Results of the Quenching Experiments

The quenched specimens were etched in a 4% solution of nitric acid in alcohol to which 1% of 'Zephiran Concentrate' (a mixture of high-molecular alkyl–dimethyl–benzyl–ammonium chlorides) was added.[28] The decomposition products of low-carbon austenite or melt etched with a light brown granular appearance. The structure darkened as the carbon content was increased, until, at about 1% carbon, a dark-etching, tempered-martensite structure was obtained. With still higher carbon content, the retained austenite in the martensite structure could be distinguished, the amount increasing with increase in the carbon concentration. A few specimens from the very high carbon alloys contained fine carbide needles, precipitated during quenching. In specimens containing less than 5·2% of silicon, ferrite that was present at temperatures near the solidus remained unchanged during the quench.

The series of photomicrographs, Figs. 9*a–i*, illustrate the various types of structure found in the quenched specimens. Figures 9*a–c* relate to alloy A1, the composition of which lies just to the left of the point H_1 of the peritectic line H_1B_1 (Fig. 6*a*). The first specimen was quenched from a temperature at which precipitation of ferrite from the melt had just started (Fig. 9*a*). Solidification was completed by the formation of an entirely ferritic structure. As the alloy cooled through the line N_1H_1, the deposition of austenite from the ferrite commenced. A specimen quenched from this region showed a duplex structure of austenite plus ferrite (Fig. 9*b*). As the temperature fell, the proportion of ferrite decreased until, on passing through the line H_1N_1, only austenite remained. As will be seen from Fig. 9*c*, the transformed austenite structure of the specimen quenched from the single-phase γ region has an appearance similar to that of bainite.

The remaining photographs of the series were obtained from specimens taken from the alloy A4. As its composition lies outside the peritectic region, no ferrite is formed from the melt. The first specimen, quenched from just above the liquidus temperature, had a tempered-martensite structure overlaid with a cellular network of retained austenite (Fig. 9*d*). The next few specimens were quenched at successively lower temperatures in the melt-plus-austenite region. The areas that were originally austenite transformed to a light-brown granular structure, and the melt transformed to a martensite-plus-retained-austenite structure. Comparison of the photomicrographs (Figs. 9*e*, *f*, *g*, and *h*) shows that as the quenching temperature is lowered, the proportion of retained austenite in the areas that were once melt increases, owing to the build-up in carbon content of the melt as solidification proceeds. The remaining specimen (Fig. 9*i*) was quenched from below the solidus and had a homogeneous tempered-martensite structure with a small quantity of retained austenite in acicular form (compare Fig. 9*d*).

Three specimens of the 7·9% silicon section (0·46, 1·00, and 1·46% carbon) were quenched from a temperature just a little higher than that of the eutectic transformation. As these specimens play an important part in the determination of the section, their structures will be considered in detail. The microstructure (Fig. 10*a*) of the 0·46% carbon alloy was essentially duplex, and consisted of grains of transformed austenite, which had a very light-etching martensite structure, and ferrite, in which a mass of fine particles had been precipitated during the quench. These were probably a carbide, but were too small to be identified. At the interface of the transformed austenite and ferrite grains, there was a thin band of a constituent, which was attacked rapidly by the etching reagent. Its internal structure was continuous with the transformed-austenite areas. It appears likely that this interfacial material represents a later stage in the transformation of the austenite to a tempered-martensite structure. The decomposition of the austenite would be expected to start at discontinuities in the structure; *e.g.*, at the grain boundaries. At first it was thought that this partial tempering had occurred during the heat-treatment for 10 sec. at 320° C. This was not the case, however, because when the heat-treatment was continued for much longer periods, there was no effect on the microstructure.

It must be assumed that the tempering took place at a higher temperature during the quench.

In the specimen from the alloy containing 1·00% of carbon, the proportion of transformed austenite was much greater, and filled about 70% of the structure (Fig. 10*b*). Within the austenite areas, there was a small quantity (5–10%) of transformed ferrite which had a structure similar to that of the 0·46% carbon specimen. A transformed-melt structure, composed of fine dendrites in a light-etching martensite matrix, surrounded the austenite dendrites.

The specimen containing 1·46% of carbon (Fig. 10*c*) had four phases at the quenching temperature: melt, austenite, ferrite, and graphite. The structures of the transformed products of the first three phases were identical with those of the two previous specimens, but the amount of dark-etching interfacial material was appreciably greater, and it completely enclosed the ferrite particles. The graphite flakes were confined almost entirely to the austenite areas.

The results obtained from the quenching experiments are plotted on the vertical sections shown in Figs. 11*a*, *b*, and *c*. They completely substantiate the arrangements of phase fields predicted by the thermal analysis experiments.

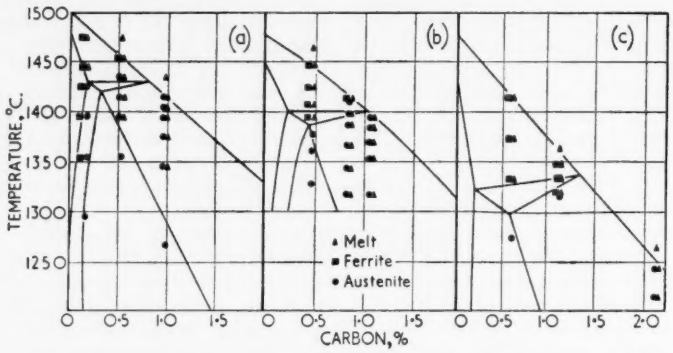


Fig. 11—Results of quenching experiments for vertical sections at (*a*) 2·3% silicon; (*b*) 3·5% silicon, and (*c*) 5·2% silicon

ACCURACY OF EXPERIMENTAL RESULTS

As previously explained, the temperatures of the liquidus and peritectic arrests for the alloys of the 2·3%, 3·5%, and 5·2% silicon sections were corrected for the deviation of their silicon content from the nominal value. For these three sections, this correction improved the consistency of the thermal data. However, in the case of the 7·9% section, the corrected liquidus temperatures show appreciable scatter, whereas the uncorrected values lie close to a straight line that intersects the temperature axis very near to the point predicted by Haughton and Becker's results.[2] Thus it seems possible that, with the higher silicon alloys, the analytical error was greater than the figure of 1·5% previously assumed, and that the alloys of the 7·9% section are closer to the nominal compositions than the reported values would suggest. The equilibrium lines for this section have therefore been drawn in accordance with the observed temperatures of the arrests.

With the exception of alloy D4, in which the kish graphite was fairly well distributed, the compositions of the eutectic alloys are thought to lie close to the composition variation curve of the eutectic reaction, as the samples for chemical analysis were taken from the middle and bottom portions of the ingots and did not, therefore, contain any appreciable amount of the hyper-eutectic graphite.

It appears probable that the 'eutectic' alloys solidified in the stable system. Consequently, the eutectic transformation temperatures observed in these alloys are a little higher than those given by the extrapolation of the liquidus and solidus lines for the section, as these lines refer to the metastable system.

The consistency of the thermal data and its agreement with the results of the quenching experiments confirm that the work was of a high order of precision. These considerations do not eliminate the possibility of a systematic error (due, for example, to contamination of the alloys). However, in view of the stringent precautions taken during the experimental work, and the fact that the extrapolated results are in excellent agreement with the accepted iron–carbon and iron–silicon diagrams, it is considered that such a possibility is remote.

Vertical Sections

From the data provided by the sections at 2·3%, 3·5%, and 5·2% silicon, it is evident that the peritecto-eutectic point lies at a higher silicon composition than that suggested by Kříž and Pobořil.[9, 10] As the accuracy with which this point can be plotted largely depends upon the location of the phase fields in the 7·9% silicon section, this section will be considered in some detail.

The temperatures of the peritectic arrests lie on a line which is nearly horizontal and which intersects the liquidus line near the 'eutectic' point. The close proximity of the points B_4 and C_4 (Fig. 6*d*) implies that the section is in the immediate vicinity of the peritecto-eutectic point; thus, it is possible that the section actually intersects the four-phase plane. If this is so, the constitution of the section between the eutectic and peritectic lines will be different from that in the previous sections. This difference will not be revealed by the thermal data, which, though locating

the peritectic and eutectic lines, gives inadequate indication of the distribution of the phase regions. To establish this portion of the section, it is necessary to consider the results of the quenching experiments.

It is known that the section lies to the left of the peritecto-eutectic point. Whether or not it intersects the four-phase plane depends upon the positions of the α and γ corners. If these lie to the right of the peritecto-eutectic point, then the section will be similar to that shown in Fig. 3*c*. If, however, the α and γ corners lie to the left of the L point, then the 7·9% silicon section may be located in one of three possible positions:

(i) If the section lies to the left of the γ corner (Fig. 3*b*), it does not intersect the plane, and its form will be essentially the same as for the lower-silicon sections

(ii) The section may cut the four-phase plane between the α and γ corners (Fig. 12*a*). A schematic vertical section for this arrangement is given in Fig. 12*b*. The single-phase austenite region has disappeared, but this phase can exist in conjunction with melt or ferrite in the two-phase regions at either side of the section, and in the three-phase peritectic and eutectic fields which are now contiguous where they meet the four-phase plane

(iii) As the section is moved through the α corner towards the peritecto-eutectic point (Fig. 12*c*), the $\alpha + \gamma$ field disappears and the peritectic region degenerates into a triangle with its apex on the liquidus surface and its base on the four-phase plane (Fig. 12*d*).

The essential difference between the sections is in the distribution of the phase regions existing above the eutectic line (or its extension). It was for this reason that the quenching temperatures for these alloys were chosen so as to lie just above that of the eutectic transformation. The microstructures of the quenched specimens revealed that the phases existing at the quenching temperature were:

Alloy D1: Austenite + ferrite
„ D2: Austenite + ferrite + melt
„ D3: Austenite + ferrite + melt + graphite.

The only section which could give rise to this phase pattern is that shown in Fig. 12*b*. It follows therefore that the α and γ corners of the four-phase plane lie to the left of the peritecto-eutectic point, and the 7·9% silicon section cuts the four-phase plane between these two corners. The presence of ferrite in the quenched specimen D3 can be accounted for by assuming that the quenching temperature coincided with that of the four-phase plane. At slightly higher or lower temperatures, the specimen would have been in the melt + austenite + graphite or the austenite + ferrite + graphite regions respectively.

The vertical section (Fig. 6*d*) at 7·9% silicon has been constructed to agree qualitatively with that given in Fig. 12*b*, and quantitatively with the positions found for the α and γ corners of the four-phase quadrilateral.

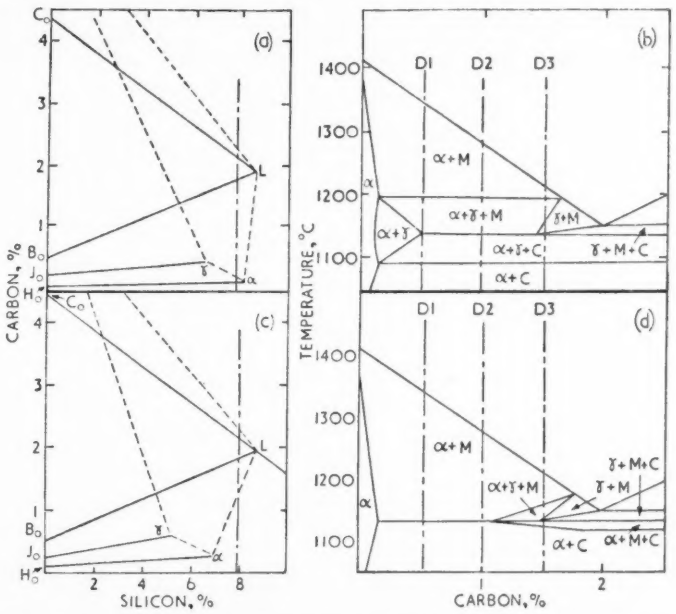


Fig. 12—Schematic basal projections and vertical sections corresponding to positions of the 7·9% silicon section between (*a* and *b*) the α and γ corners of the four-phase quadrilateral, and (*c* and *d*) the α and L corners of the four-phase quadrilateral

Basal and Other Projections of the System

The experimental data relating to the points H, J, B, E, and C are summarized in Table V. Values for H, J, and B at 0% silicon have been taken from Adcock's diagram[21] and the points E and C from Austin's diagram.[5]

Projections of these points on the basal plane and on planes parallel to the iron–silicon and iron–carbon sides are given in Figs. 13*a*, *b*, and *c* respectively.

The Peritecto-Eutectic Point—This point lies at the intersection of the eutectic and peritectic curves. The

projections on the basal plane (Fig. 13*a*) of the five eutectic points lie close to a straight line, the equation of which is

$$C\% = 4{\cdot}3 - Si\%/3{\cdot}48 \quad \ldots\ldots\ldots\ldots\ldots\ldots (1)$$

This equation may be compared with the similar one deduced for the stable system by Hanemann and Jass[11, 12]:

$$C\% = 4{\cdot}23 - Si\%/3{\cdot}2 \quad \ldots\ldots\ldots\ldots\ldots\ldots (2)$$

(valid up to 3% silicon)

The projections of the eutectic points on the vertical planes (Figs. 13*b* and *c*) indicate that the temperature of the transformation increases up to a maximum at about 5·0% silicon, and then decreases until it meets the four-phase plane.

The composition variation of the melt phase (B_0L) of the peritectic transformation is sensibly linear up to about 5% silicon, after which it turns more sharply towards the peritecto-eutectic point. From the intersection of the peritectic and eutectic curves on the three projections, the co-ordinates of the peritecto-eutectic point are deduced to be 8·35% silicon and 1·92% carbon at 1145° C.

The α and γ Corners of the Four-Phase Plane—The γ corner, as defined by the intersection of the lines $J_0\gamma$ and $E_0\gamma$ on the basal projection, lies at 7·2% silicon and 0·6% carbon. Because of the difficulty in determining the end of the peritectic transformation, the temperature variation of the point J could not be accurately established. Consequently, the projections on the vertical planes cannot be used to confirm this position.

The projections of the lines $H_0\alpha$ and B_0L on the iron–silicon side are very nearly coincident, the two lines being separated by only a few degrees. Consequently, the silicon ordinate of the point α will be only slightly less than that of the L point; *i.e.*, α will be close to its limiting position. This is confirmed by the fact that on all four of the vertical sections, the line HB of the peritectic field departs only slightly from the horizontal.

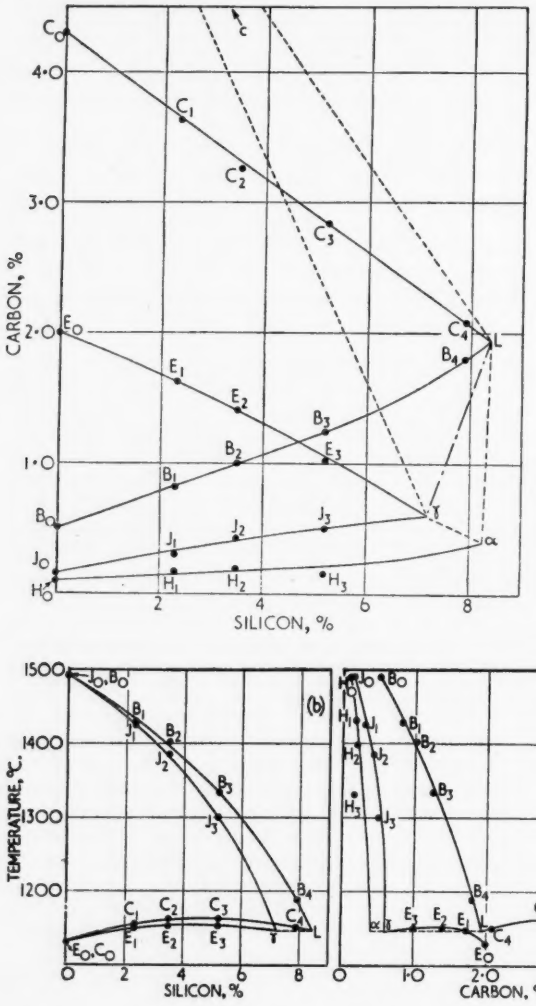


Fig. 13—Projection of the metastable diagram on: (*a*) The basal plane, (*b*) the Fe–Si binary side, and (*c*) the Fe–C binary side

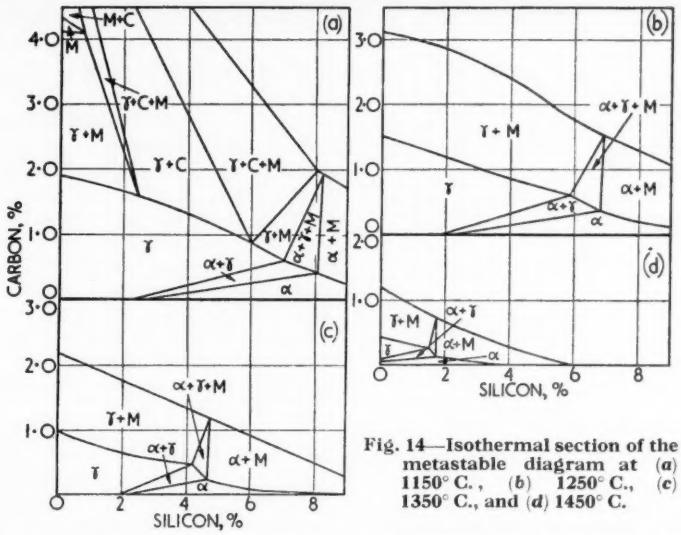


Fig. 14—Isothermal section of the metastable diagram at (*a*) 1150° C., (*b*) 1250° C., (*c*) 1350° C., and (*d*) 1450° C.

It is not possible to assess accurately the carbon content of the corner from the projection of the curve $H_0\alpha$ on the iron–carbon side, because the data obtained for the carbon variation of the point H are not sufficiently reliable. However, limiting positions for the α corner can be deduced by consideration of the following facts:

(i) On the constant-silicon vertical sections, the point H must lie to the left of the point J. Thus, unless there is an abnormal change in direction of the curve $H_0\alpha$, the carbon content at α must be less than that at γ

(ii) The alloy D1 of the 7·9% silicon group contained only ferrite and austenite at a temperature just above the four-phase plane; hence, the composition of this alloy must lie on the low-carbon side of the projection of the $\alpha\gamma$ edge of the four-phase quadrilateral.

To satisfy these two requirements, the α corner must lie in the range 0·4–0·6% carbon. As the minimum of this range is in agreement with the composition variation of the curve $H_0\alpha$, the most probable position for the α corner is 0·4% carbon and 8·25% silicon.

The Carbide Corner—The temperature and composition variation determined for the equilibrium lines C_0L and $E_0\gamma$ correspond to the formation of the austenite–cementite eutectic.[26] The edges Lc and γc of the four-phase quadrilateral have therefore been drawn to intersect at the point c (Fe_3C) on the binary iron–carbon side.

ISOTHERMAL SECTIONS OF THE SYSTEM

Four isothermal sections at 1150°, 1250°, 1350°, and 1450° C., respectively, are shown in Figs. 14*a*, *b*, *c*, and *d*. The sections include only those regions which have been determined by the present investigation or which can be deduced directly from the form of the binary sides. The liquidus–graphite surface is not shown, because the available data are scanty and unreliable.

As the section moves down the temperature axis, the area of the single-phase liquidus region decreases and the three-phase peritectic region $\alpha + \gamma +$ melt moves towards the four-phase plane. The most interesting section, and the only one that requires comment, is the one which passes just above the four-phase plane (Fig. 14*a*). Because of the convexity of the 'eutectic' temperature curve, this section intersects the 'eutectic' region twice: near the iron–carbon side and in the vicinity of the four-phase plane. The two $\gamma +$ melt + carbide regions are separated by an austenite + carbide region.

CONCLUSIONS

The invariant compositions of the phases: melt, austenite, ferrite, and carbide (which define the corners of the four-phase plane in the metastable system) are given in Table I. The most significant difference between these results and those obtained by Kříž and Pobořil is in the position of the peritecto-eutectic point with respect to the α and γ corners.

Table V

TEMPERATURE AND COMPOSITION VARIATION OF POINTS H, J, B, E, AND C

Si, %	H		J		B		E		C	
	C, %	Temp., ° C.	C, %	Temp., ° C.	C, %	Temp., ° C.	C, %	Temp., ° C.	C, %	Temp., ° C.
0	0·10	1492	0·16	1492	0·51	1492	2·00	1130	4·30	1130
2·30	0·18	1430	0·30	1425	0·82	1429	1·62	1148	3·63	1150
3·50	0·20	1397	0·42	1385	1·02	1402	1·40	1152	3·26	1162
5·20	0·16	1325	0·50	1300	1·24	1333	1·00	1152	2·84	1162
7·90	0·36	1188	...	...	1·80	1188	...	...	2·08	1150

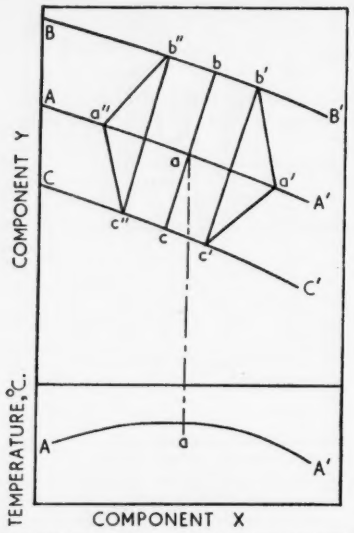


Fig. 15—Projection of the equilibrium triangles when a maximum occurs in the temperature variation of a ternary eutectic transformation

As previously mentioned, the configuration given to the quadrilateral by the earlier investigators imposes a restriction on the minimum silicon content of the carbide corner. The results of the present investigation show that the peritecto-eutectic point lies on the other side of the plane, and that the carbide corner can now be placed at the composition of cementite, in accordance with the results of recent X-ray diffraction[17] and chemical[18] studies of the carbide.

In the discussion of the qualitative nature of the system, it was shown that this movement in the position of the peritecto-eutectic point requires the eutectic three-phase field to approach the four-phase plane from the top. In this respect, the curves given for the temperature variation of the eutectic transformation (Figs. 13*b* and *c*) are particularly significant. The temperature of this transformation increases from 1130° C. on the iron–carbon side up to a maximum of 1160° C. at 5% silicon. Thereafter, it decreases until it reaches the four-phase plane at 1145° C. The form of the eutectic curve is thus in agreement with the results (Fig. 5*a*) of earlier investigators who found a sharp increase in the temperature of the eutectic between 0 and 2% silicon. At the same time, it satisfies the theoretical requirements introduced by the geometry of the four-phase quadrilateral.

The occurrence of a maximum in the temperature variation of a eutectic is unusual but not theoretically impossible. The alteration in the orientation of the equilibrium triangle which accompanies the change in the direction of the curve is illustrated in Fig. 15. On either side of the maximum, the melt corners (a'', a') lie in the direction of falling temperature. As the two triangles approach one another, the melt corners recede with respect to the two solid-phase corners until the maximum of the curve is reached, when the triangles degenerate into an isothermal straight line *bac*, which is the conode connecting the compositions of the three phases. This transformation is very similar to that found in the case where a eutectic changes to a peritectic without passing through a four-phase plane.

Acknowledgments

The authors wish to acknowledge the assistance given by the British Cast Iron Research Association, who carried out the chemical and spectrographic analysis of the alloys; the National Physical Laboratory, who supplied the Swedish iron and its analysis; St. Lawrence Alloys and Metals, Ltd., who supplied the high-purity silicon; and The Mond Nickel Co., Ltd., who provided the sample of pure nickel.

They are grateful to Dr. S. J. Kennett, Dr. J. G. Pearce, and Mr. H. Morrogh for their encouragement and advice, and to Miss B. Silvester for her assistance with the photography.

One of the authors (J.E.H.) has received financial assistance from the Department of Scientific and Industrial Research and held a Munitions Committee Fellowship awarded by the University of Liverpool.

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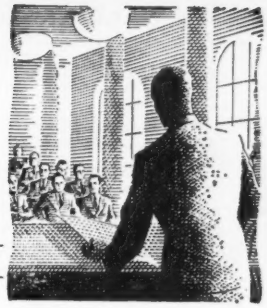
DISCUSSION ON PAPERS



DISCUSSION AT MEETINGS · WRITTEN CONTRIBUTIONS

Correspondence on the Paper—

THE STRUCTURE OF CARBIDES IN ALLOY STEELS—Part II*

By H. J. Goldschmidt

Dr. **F. R. Morral** (Kaiser Aluminium and Chemical Corporation, Washington, U.S.A.) wrote: The statement (p. 193) that " The occurrence of κ-carbide [$(W,Fe)_{23}C_6$] in high-speed steels has previously been neglected " (before 1949) is not altogether correct. This carbide was noted and reported in 1933.†

On page 199 the author calls attention to a contracted η-carbide $(Fe,W)_6C$, with a lattice parameter of 10·958 kX., compared with the usual values of 11·075–11·064 kX. This contracted η was obtained on heating the double carbide.

I also noted this contraction in 1934, when, on heating a double carbide with a lattice parameter of 11·08 Å. at 1000° C. for 48 hr., it contracted to 10·94 Å. It may be of interest to mention now that this phase with 10·94 Å. was also found on samples of double carbide powder which had been ageing (?) in the laboratory for some 7 years. In this instance both patterns with lattice parameters 10·94 Å. and 11·08–11·11 Å. were together. It is thought that possibly these are the extreme solubility limits for the $(Fe,W)_6C$ phase under equilibrium conditions. Mr. Goldschmidt gives values for the limits of η-carbide as 11·0547 and 11·077 kX., while Dr. Westgren‡ has mentioned 11·04–11·08 Å.

It appears that more study of equilibrium conditions for these systems and the respective carbides is necessary.

Monsieur **A. Michel** (Chambre Syndicale des Producteurs d'Aciers Fins et Spéciaux) wrote: I have read with great interest Part II of Mr. Goldschmidt's paper. It is the result of considerable work, and the author is to be congratulated.

As I and my colleague M. Papier are at present studying transformations in high-speed steels, I should very much like to bring several points to Mr. Goldschmidt's notice, and to ask him for some explanations.

It seems that the author has used, without modification, Mr. Cohen's electrolytic method. M. Papier has used the same method for certain high-speed steels, but with some modifications that he considered necessary for quantitative determination of both the nature and the importance of the residue. (This matter is to be published.) I would therefore ask the author whether he found any inconsistencies in the results obtained by using this method.

Mr. Goldschmidt indicates that after oil-quenching at 1330° C. and tempering for 2 hr. at 550° C., steel type 18–4–1 resolves itself into 70–75% of martensite, 5–10% of austenite, and 20–25% of carbides. But the structure is far more complex than this; after quenching, it is composed of carbides (undissolved after austenization), martensite, and residual austenite. The tempering results in martensite with a carbide precipitation, and possibly in structural changes in the residual austenite, and the cooling results in a partial transformation of the residual austenite to martensite (*i.e.*, secondary martensite).

Has the author any further information on martensite precipitations at temperatures below 550° C. ?

Mr. Goldschmidt also indicates that, after the treatment, practically no tungsten or carbon were present in the matrix (large deposits of these elements had been found in carbides). But after such treatment, the matrix of high-speed steels is still particularly resistant to heat-softening (the usual treatment). It therefore seemed that there was this contradiction: that steel with low C and W content was very resistant to heat-treatment. In our experiments using Cohen's extraction method, we found only very little C and W in the matrix; but in examining a steel of a composition the same as the calculated composition of the matrix, we found that it softened considerably. Closer studies of carbide extraction led to far more logical results, showing in particular that the matrix has a much higher W content than was originally supposed.

In his explanation of the stability of austenite through its similarity to schizophrenia, it seems that the author anticipated that there would be no modification of the austenite for a long time, followed by a certain change at a predetermined time. If such a thing can take place in human actions, it is because the mental process can wait a certain time before ordering the carrying out of the physical act; but nothing of this sort seems possible to us in purely physical phenomena. It therefore seems certain that during the incubation period changes do

* *J. Iron Steel Inst.*, 1952, vol. 170, pp. 189–204.

† F. R. Morral, G. Phragmén, and A. Westgren, *Nature*, 1933, vol. 132, July 8, pp. 61–62.

‡ A. Westgren, *Jernkontorets Ann.*, 1933, pp. 1–14.

occur, although they are undiscovered by our methods of investigation, rather in the same manner as in an illness.

Finally, on p. 201, Mr. Goldschmidt says that after quenching from 1330° or 1250° C., there is still the same amount of carbides as in the first alloy. This seems definitely to contradict all known facts, and especially Cohen's calculations on high-speed steel 6–6–4–2. Actually, η-carbide is considered to possess the most W, and to be such that its insertion in solution in the matrix at high temperatures permits the inclusion of W in austenite, and therefore in martensite. The author's conclusions seem to point to the fact that there is very little W in the martensite in high-speed steels. As already stated, this fact does not agree with our own results, and cannot be accepted without further proof.

AUTHOR'S REPLY

Mr. **H. J. Goldschmidt** (B.S.A. Group Research Centre, Sheffield) wrote in reply: I thank Dr. Morral for drawing attention to his earlier work. My remark that the occurrence of the $\varkappa$-carbide in high-speed steels had previously been neglected did not intend to imply that this was an original result, but that it appeared to have often been ignored in work on high-speed steels and especially on the relevant equilibrium diagrams. I apologize for the omission of the reference to Morral, Phragmén, and Westgren's 1933 letter. The pioneer work of Westgren's school has otherwise, I hope, been fully acknowledged in Parts I and II.

That the contracted η-carbide had also been obtained in Dr. Morral's previous experiments, was indeed new and interesting to me; this result does not appear to have been published. It is probable that his two η-carbides of lattice parameters 11·08 and 10·94 Å. would correspond to ours, and I am pleased that such confirmation is available. The theoretical interpretation of the two phases is, however, doubtful, and I fully agree that the equilibria in this system require much further investigation; it is possible that the phases correspond to the tungsten and iron-rich limits of the η-field, in which case Westgren's Fe_3W_3C and Fe_4W_2C limits would have to be extended towards higher iron contents, and a similar interpretation of η^* having a composition considerably richer in iron has also been suggested in the paper (pp. 199–200). I would, however, not exclude loss of interstitial carbon (*see* Possibility (ii), p. 199) as a possible subsidiary factor; it would also be interesting to know whether free tungsten appeared in Dr. Morral's experiments, as in ours, with the η-contraction.

I much appreciate M. Michel's contribution and I am certainly looking forward to seeing the publication of his and M. Papier's work.

As M. Michel says, the method of electrolytic extraction employed was essentially that of Cohen: 5% HCl was used at a current density of about 0·2 amp./sq. in. over a period of 24–36 hr., the residue being thoroughly washed in distilled water, ether and alcohol, and vacuum-dried. We believed our method to be adequate and, at any rate, the best possible; and we found no inconsistencies. However, I shall be most interested to know M. Papier's modifications of the method.

My remark that after quenching the 18–4–1 type steel from 1330° C. and tempering for 2 hr. at 550° C., its structure consisted of 70–75% of martensite, 5–10% of austenite, and 20–25% of carbides, was quite a general statement, which would seem in no way to conflict with M. Michel's further amplification as to how each of these constituents are subdivided in their development during heat-treatment history. The statement is, I suggest, still correct, and at that stage it was not intended to deal with details. I fully agree that the detailed history of each constituent is highly complex, and I agree with M. Michel's analysis in this respect. I have at present no further information regarding precipitations below 550° C.

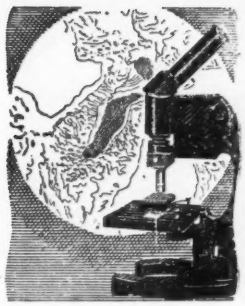
Regarding M. Michel's comments on the presence of tungsten and carbon in the matrix, even after tempering, so that it is rendered resistant to heat-softening, may I say that a small amount of W and C had still remained in the matrix in our example also (*see* atomic formula, p. 191); and I suggest that a composition of this type may well explain the matrix properties referred to, not only through the presence of W but also through that of Cr and V. Moreover, the possibility is not excluded that on varying the manner of tempering treatment, the W content in the matrix becomes higher than in this instance—to account for M. Michel's higher value—particularly as some W resides not only in the η-carbide but also in $\varkappa$, and the latter is more readily soluble in the matrix. The fact that the W contents in the matrix—at least in our case—could not have been high is indicated by the lattice-parameter values of ferrite (Table II, p. 192), which are not very much higher than the pure iron value ($a_\alpha = 2{\cdot}8610$ kX.), the increase being largely attributed to the Cr in the matrix. Considerable amounts of W in solution would be expected to give a much more expanded lattice (as was indeed the case in our decarburized steels).

Like most similes, the comparison with schizophrenia in discussing the impediments to austenite breakdown must not, of course, be taken too literally. However, in this instance it applies even to the extent that M. Michel indicates. It is not implied that there is no change in the austenite matrix for a predetermined time, but that there is no outwardly apparent change. It is quite probable—and was indeed meant to be conveyed—that incipient nucleation on an atomic scale takes place within the matrix, previous to precipitation of phases, but that delay in actual precipitation is caused by the fact that in different regions of the lattice (predetermined by heterogeneities in composition), a tendency occurs for different competitive phases to nucleate, owing to their possessing only a slight difference in lowest free energy. It is visualized that different configurations in atomic structure occur in different adjoining regions of the parent lattice; this would cause 'jamming' to the actual progress of nucleation and growth of any one phase until, after a process of 'attrition,' the growth of the phase of lowest free energy could exert itself over the metastable phases. It is believed that analogous processes are known in human affairs, but the comparison was, of course, only meant lightly, and I suggest that it should not be pursued.

Regarding M. Michel's last point, there does not seem to be any contradiction of facts. It is concluded from the experiment referred to that the bulk of η-carbide is still free carbide even at 1330° and 1250° C., but it was implied that the η-carbide only—and not the 'carbides'—remained mainly undissolved at austenizing temperatures. It is considered that $\varkappa$-carbide and, where applicable, others (*e.g.*, the NaCl type), as well as a small amount of η, enter completely into austenite solution. As all these carbides bear some W, the austenite and therefore martensite are still capable of containing appreciable amounts of W, in agreement with M. Michel's assertion.

B. I. S. R. A.

BRITISH IRON AND STEEL RESEARCH ASSOCIATION



Speed-Dependent Variables in Cold Strip Rolling

By R. B. Sims, B.Sc., A.M.I.Mech.E., A.Inst.P., and D. F. Arthur, B.Sc.

SYNOPSIS

The various ways in which the mill speed may influence the specific roll load and strip thickness in cold rolling are considered, and an experiment is described which allows the speed-dependent variables to be separated. In the experimental work, both steel and copper strip were used with thicknesses on entry between 0·050 in. and 0·010 in., and a range of mill speeds between 10 ft./min. and 300 ft./min. The strip lubricants were a commercial rolling oil and colloidal graphite. The results show that the variation of the coefficient of friction between the rolls and strip is the principal cause of load and thickness changes, and a relationship between the mean coefficient of friction and the speed of the mill is derived from the experimental results.

Methods of controlling the gauge of strip during periods of acceleration and deceleration are discussed, and it is shown that the methods of automatic control suggested by Hessenberg and Sims will maintain gauge to close tolerances over the speed range of the experimental mill.

Introduction

A CHARACTERISTIC of the high-speed strip mill is the dependence of the thickness of the rolled strip on the speed of rolling. It is found that the strip decreases in thickness as the mill is accelerated and a corresponding increase occurs as the mill is decelerated. The changes in gauge due to speed variations may be considerable. During tests on a 5-stand tandem tinplate mill, Stoltz and Brinks[1] found that strip leaving the last stand at 0·013 in. at the threading speed of 400 ft./min. decreased in thickness to 0·0088 in. when the mill was accelerated to its operating speed of 3500 ft./min., results which are in close agreement with the earlier observations by Mohler.[2] With the rapid increase in rolling speeds in the past 15 years, the variations in the thickness of strip due to changes in the mill speed have gathered importance, and are now probably the largest of the gauge variations in cold rolling.

The measures taken to control this speed effect have been palliative rather than remedial. The length of strip forming the coil* has been increased and the acceleration and deceleration times reduced, so that the material at the ends of the coil, which is outside the British Standard Specification tolerances for cold reduced strip, is usually about 5% of the total length of strip. This still represents a considerable loss in production. Moreover, with the increase in coil weight and mill motor power there has been a corresponding increase in the cost of plant and in the engineering difficulties of design and maintenance of the mill, its drive, ancillaries, and handling equipment.

Methods of eliminating the speed effect from the rolling process can be suggested on a systematic basis only after its mechanism has been explained. The aim of the experiments described in this paper has been first to determine the speed-dependent variables, then to obtain a quantitative estimate of them over the range of speeds provided by the equipment available, and finally to suggest ways in which the speed effect may be controlled.

* Strip rolling is a batch process and the coil is, in general, the largest unit of production that can be rolled without altering the speed of the mill, apart from the initial acceleration and the deceleration at the end of the run.

Paper MW/A/80/51 of the Rolling Committee of the Mechanical Working Division of the British Iron and Steel Research Association, received 2nd July, 1952. The views expressed are the authors' and are not necessarily endorsed by the Committee as a body.

Mr. Sims is Head of the Rolling Mill Laboratory, and Mr. Arthur is a member of the Cold Rolling Team of the Mechanical Working Division of the Association.

Notation

F = total roll load, tons
B = mean strip width, in.
H = entry thickness of strip, in.
h = exit thickness of strip, in.
R = work-roll radius, in.
R' = effective radius of curvature of deformed roll in the arc of contact, in.
S_0 = minimum distance between the work rolls before the strip is entered = the roll setting, in.
δ = draft = $H - h$, in.
r = reduction in pass = δ/H expressed as a percentage or fractionally
f = forward slip expressed as a percentage or fractionally
$\bar{k}$ = mean yield stress of the material, tons/sq. in.
M = elastic coefficient of mill, tons/in.
M_R = elastic coefficient of rolls, tons/in.
M_H = elastic coefficient of the housings, screws, etc., tons/in.
$\bar{\mu}$ = mean coefficient of friction
α = angle of contact, rad.
ϕ = angular co-ordinate of neutral plane
U = peripheral speed (ft./min.) of the rolls (the mill speed)

For any point in the arc of contact :

θ = angular co-ordinate, rad.
y = strip thickness, in.
μ = coefficient of friction
k = yield stress in plane compression, tons/sq. in.

ORIGIN OF THE CHANGES IN GAUGE OF ROLLED STRIP

Hessenberg and Sims[3] have shown that the changes in gauge of strip during rolling occur either as a result of changes in the setting of the rolls or because of elastic distortions in the mill components, due to changes in the rolling load. The thickness h of the rolled material is related to the roll force F and the elastic deformation of the mill by the equation[3]:

$$F = M(h - S_0) \quad \text{.....(1)}$$

where M is the elastic coefficient of the mill, and S_0 is the roll setting, or the minimum distance between the working faces of the rolls when there is no strip in the mill. The roll force required to effect the plastic deformation of the material is dependent on the radius

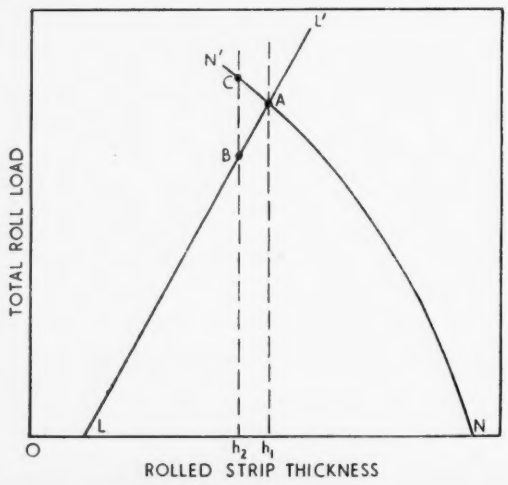


Fig. 1—An F/h diagram

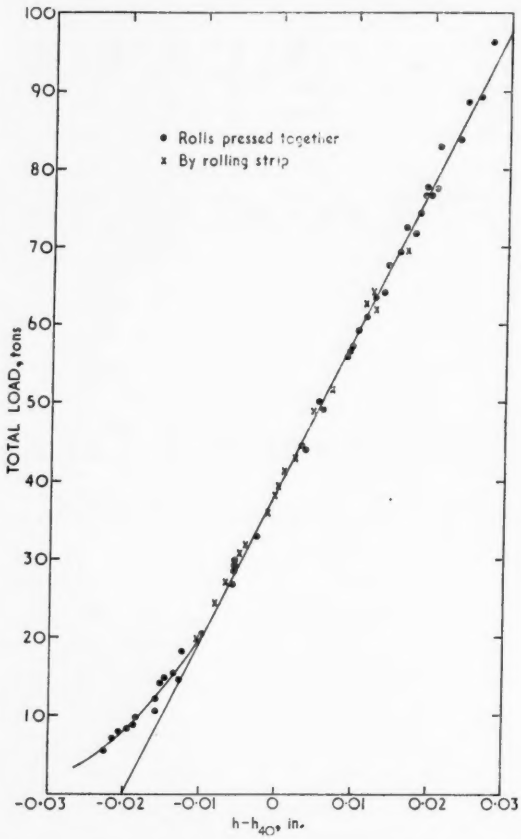


Fig. 2—Relation between load and roll gap extension

of the work rolls R, the entry thickness H, the reduction in thickness effected in the pass r, the width of the strip B, the yield stress $\bar{k}$, and the coefficient of friction between the strip and the rolls μ. An approximate relationship between these variables, which gives close agreement with experiment, has been suggested by Bland and Ford[4] and it may be written in the form:

$$F = B\sqrt{(R'\delta)}.\bar{k}f(a,r) \quad \text{.....(2)}$$

where $\bar{k}$ is a mean yield strength for the material for the range of deformation in the pass; $a = \mu\sqrt{(R'/h)}$, a dimensionless parameter; and the function $f(a,r)$, the form of which has been given by Bland and Ford, provides the contribution of friction to the total load.

It is clear that equations (1) and (2), which have been called the elastic and the plastic equations, respectively, must be simultaneously satisfied during rolling, and the solution may be represented on a diagram with F and h as co-ordinates for given values of other variables.[3, 5] Figure 1 shows such a diagram, in which LL' represents the elastic equation and NN' the plastic equation for one set of rolling conditions. The point of intersection A gives the mill load and exit thickness of the strip. If the thickness changes from h_1 to h_2, owing to a variation in the rolling speed, then either the variables of the plastic equation must

alter to give a new point of intersection with the elastic equation at B, or the elastic equation must alter to give C as the point of intersection. One or more of the variables in the elastic and/or plastic equation must be speed-dependent, since the rolling speed does not occur explicitly in either equation. The problem, therefore, is to devise an experiment to determine these variables and obtain an estimate of the relationship between them and the rolling speed.

POSSIBLE SPEED-DEPENDENT VARIABLES

In the literature on the cold rolling process, four factors are mentioned as possible causes of the speed effect. They are: the oil film thickness in hydrodynamically lubricated roll neck bearings, roll distortion, the yield strength of the strip, and the coefficient of friction in the arc of contact. The effect on gauge of possible changes in these factors with mill speed will now be examined.

Oil Film of Hydrodynamically Lubricated Bearings

Experiments carried out when the B.I.S.R.A. experimental mill was fitted with hydrodynamically lubricated bearings showed that the thickness of the oil film separating the roll neck and the chock increased with mill speed, and a measure of the effect has been obtained. Sims[6] has shown that a change in the oil film thickness is equivalent to a decrease in the roll setting S_0. The elastic line LL' on the F/h diagram will be displaced towards the origin as the mill is accelerated, so that the strip thickness decreases while the roll load increases. This is not the only cause of the speed effect in rolling, however, since changes in gauge with speed have been observed in mills not equipped with these bearings.

Roll Distortion

Calculations based on von Kármán's[7] equation for the rolling process indicate that very large stresses are imposed on the roll in its arc of contact with the strip. This is particularly the case when rolling materials such as thin, fully strain-hardened stainless steel or the high-carbon steels, and has led to the suggestion by Mohler[2] that the speed effect is, in some way, due to elastic hysteresis in the heavily loaded rolls. From an examination of his experimental results, Ford[8] concluded that a change in the radius of curvature of the rolls in the arc of contact was a possible cause of the speed effect. Subsequently, Keller[9] drew attention to the fact that at the high pressures in the roll gap there is a possibility that the material of the rolls would yield plastically below the surface, and the amount of plastic deformation may depend on the strain rate and hence on the mill speed. The effect of the combined elastic and plastic deformation would tend to lower the average value of the modulus of elasticity for the material of the rolls close to the roll gap, and so increase the length of the arc of contact.

These hypotheses cannot at present be verified experimentally, since a method has not been found for measuring the curvature of the arc of contact. Changes in the values of the modulus of elasticity, however, may be detected since, in addition to changes in the curvature over the arc of contact, the rolls also distort as a whole under load, so that the diameters in the plane containing the roll axes decrease in length.

The elastic coefficient of the mill M represents the load required to separate the rolls by unit length against the elastic restraint exerted by the mill housings, the screws, and the rolls. If the effective elastic coefficient of the rolls M_R is defined as the load in the arc of contact required to decrease by unit length the diameters of both work rolls in the plane of the roll axes, and if M_H is the corresponding elastic coefficient of the mill housings, then $M = M_H M_R/(M_H + M_R)$.

If the modulus of elasticity of the rolls increases with speed, the elastic constants of the rolls and the mill will increase, and the radius of curvature in the arc of contact will decrease by an amount which may be calculated from Hitchcock's equation.[10]

The value of the elastic coefficient M of the experimental mill has been measured by passing strips of

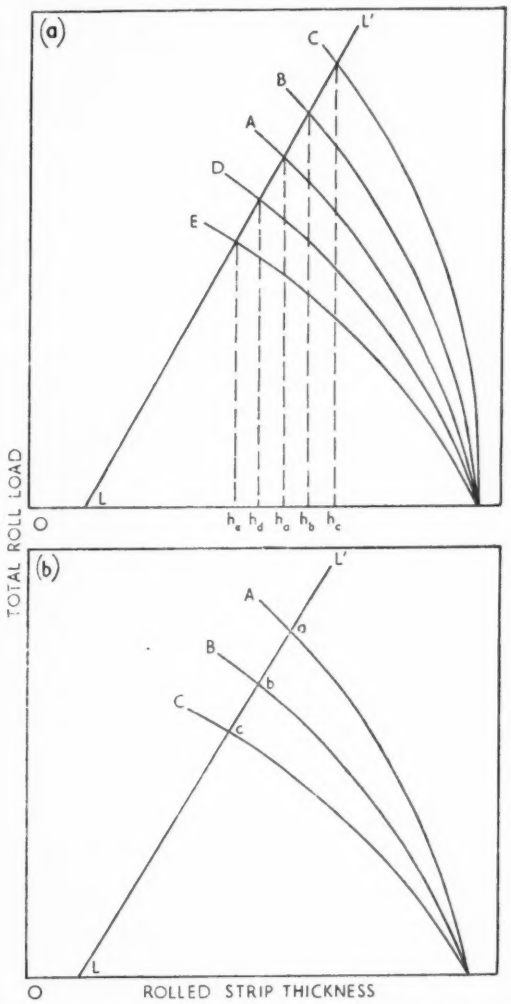


Fig. 3—Effect of yield strength and coefficient of friction on rolled strip thickness: (*a*) Yield strength; (*b*) coefficient of friction

varying thickness through the mill without changing the roll setting. The slope of the graph of roll-load/rolled-strip-thickness will then give the value of M. Alternatively, the rolls may be pressed together and the applied load plotted against roll setting. The latter experiment has been repeated at various speeds between 10 and 300 ft./min. The results of several experiments by both methods are shown in Fig. 2, in which the measurements of rolled strip thickness or roll setting have been reduced so that the line drawn through each set of experimental values passes through the point (0,40) on the diagram. There is very little scatter in these results at mill loads above 20 tons, and there is no evidence of changes in M with speed.

Yield Stress of the Strip

In the plastic equation the roll load is proportional to the mean yield strength of the material, which in turn is a function of the instantaneous values of the yield strength along the arc of contact. In the F/h diagram in Fig. 3*a*, curves B and C show diagrammatically the effect of increasing the value of the mean yield strength in the plastic equation of curve A, whilst curves D and E are obtained when the mean yield strength is decreased. If the straight line LL' represents the elastic equation at a particular setting of the rolls, then h_a, h_b, etc., will represent the exit thickness of the strip at the different mean yield strengths. It will be seen that the mean yield must decrease with speed to produce a decrease in the exit thickness. The question of whether the yield stress does, in fact, decrease when the rolling speed is increased is discussed on p. 292.

Coefficient of Friction Between Rolls and Strip

The coefficient of friction between the rolls and the strip has never been measured directly; it occurs only in the plastic equation, where a decrease in friction will decrease the roll load. Its effect on the rolled strip thickness is demonstrated in the F/h diagram of Fig. 3*b*. The curves A, B, and C represent qualitatively the effect of a progressive reduction in the coefficient of strip friction when rolling a strip of constant entry thickness. The line LL' represents the elastic equation at a roll setting represented by OL. The co-ordinates of points of intersection a, b, and c give the roll load and rolled strip thickness in each case. If the coefficient of friction is the speed-dependent variable, it must decrease as the mill speed is increased, in order to decrease the rolled strip thickness.

EXPERIMENTAL INVESTIGATION

The experiments to determine the speed-dependent variables were carried out on the B.I.S.R.A. 10 in. × 10 in., 2-high cold strip mill, which is equipped with full Ward-Leonard speed control and has a maximum speed* of 307 ft./min. when the mill motor is lightly loaded. Stable mill speeds as low as 10 ft./min. may be obtained when the controls are carefully adjusted.[11] The mill is accelerated by a motor-driven rheostat, which first strengthens the generator field and then weakens the motor field. For these experiments this acceleration period was increased from 10 sec. to about 40 sec.

Loadmeters of the type described by Sims, Place, and Morley[12] were fitted under each of the loading screws. These loadmeters are essentially cylinders of high-tensile steel on which are bonded electric resistance strain gauges connected to form a load-sensitive, temperature-compensated Wheatstone network. The networks were supplied with highly stabilized 100 V. D.C. and the output current proportional to the load was measured directly on microammeters. These were photographed at 11 frames/sec., together with a meter indicating the mill speed, and a stop watch graduated to 0·01 sec. An accuracy of $\pm$ 1% on the measurement of mill load was obtained by this method. The thicknesses of the rolled materials were measured by an electrical contact strip micrometer graduated to 0·0002 in. In experiments on coils the thickness of the strip was measured separately after the pass, the coil being transferred at moderate tension from one reel to the other while the mill rolls were lifted out of contact with it. Where short lengths of strip were rolled, samples approximately 1 in. square were cut from the specimen after rolling and were held freely between the anvils of the micrometer while the measurement was taken.

Experiments were carried out on coils of steel strip and short lengths of copper strip. Their yield stresses in plane compression are given in Table I. A single coil of commercial-quality killed steel, with a carbon content of about 0·10%, and initially 0·080 $\pm$ 0·001 in. thick, 3·0 in. wide, and containing about 400 ft. of strip, was used for the work. It was prestrained by rolling down to 0·050 in. thick and was then divided into two coils. In the first experiment a rolling oil was applied freely to both sides of one of the coils and to the rolls during the pass. For the second

* The surface speed of the rolls is commonly termed the mill speed and throughout this paper is denoted by U. The speed of the rolled strip is then, by definition, $U(1 + f)$, where f is the forward slip.

Table I

YIELD STRESS IN PLANE COMPRESSION

Values are in tons/sq. in.

Material	Reduction, %								
	0	10	20	30	40	50	60	70	80
Mild steel	11·6	29·8	36·8	41·1	44·0	46·2	47·8	48·8	49·7
H.C. copper	7·6	16·5	21·2	24·1	26·0	27·4	29·6	29·8	31·0

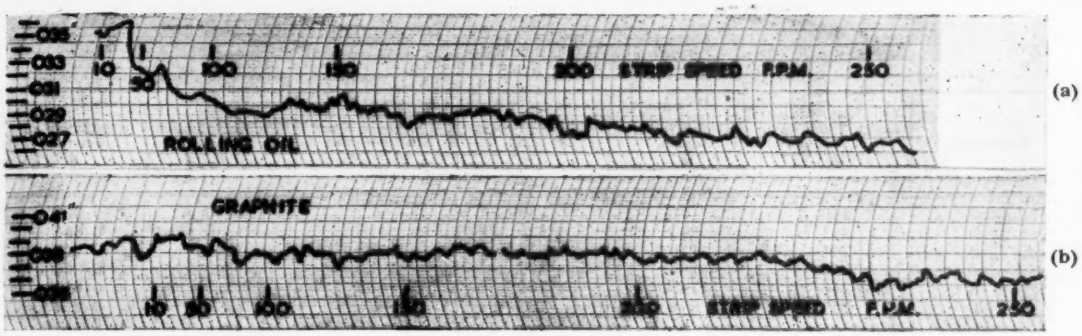


Fig. 4—Variation in thickness along steel coils after rolling: (*a*) Using rolling oil; (*b*) using graphite

experiment the rolls, mill guides, and strip were carefully degreased with magnesium oxide, sprayed with a suspension of Acheson's colloidal graphite dispersion No. 80 in alcohol, and allowed to dry. The mill was set up at a speed of 10 ft./min. to take a 30% pass on the coil, and was then accelerated as smoothly as possible to its maximum speed. Tensions were not used in either experiment. The coils were paid off freely from the drag reel and were coiled after the pass; the effects of variations in tension on gauge[3] were thereby eliminated.

The copper coils, initially 0·060 in. thick, 3 in. wide, and 200 ft. long, were rolled down to five lengths (0·045, 0·037, 0·030, 0·022, and 0·015 in. nominal thickness) in a series of passes, using the tension method of gauge control[13] to obtain a close tolerance. From each of these lengths, sixteen strips 15 ft. long were cut, and at the centre of each two transverse lines were scribed to form a reference length. The widths and lengths of the references were accurately measured before rolling. These lengths and widths were again measured after rolling, and 10 pieces about 1 in. square were cut at equal intervals on diagonals 33 in. long within the reference lengths, and measured by the contact strip micrometer. The thickness of the rolled strip was taken as the mean of these 10 readings, thereby reducing the errors due to roll eccentricity. The thickness of the strip before rolling was calculated from the changes in the length and width of the reference length, and the measured rolled thickness, assuming that the material of the strip did not change in volume during the reduction. Each set of sixteen lengths was further subdivided into two groups of eight strips of matched thicknesses. One group was lubricated with rolling oil, the other with colloidal graphite in alcohol, using methods described above. The mill was adjusted to take approximately a 30% reduction at a speed of 10 ft./min. on each group of strips. The first strip was rolled at 10 ft./min., the second at 30 ft./min., and the remainder at 50, 100, 150, 200, 250, and 300 ft./min. The actual speeds in the experiments differed slightly from these values, owing to the drop in mill speed as the strip was entered.

DISCUSSION OF EXPERIMENTAL RESULTS

The roll necks of the experimental mill were equipped with self-aligning roller bearings for the present series of experiments. Of the possible speed-dependent variables discussed on p. 287, only roll distortion or changes in the yield strength or in friction could have caused systematic changes in strip thickness with speed.

The variation in thickness along the two steel coils after rolling is shown in Fig. 4, and the results of the experiments on the short lengths of the copper strip are shown in Fig. 5. The measurements made during

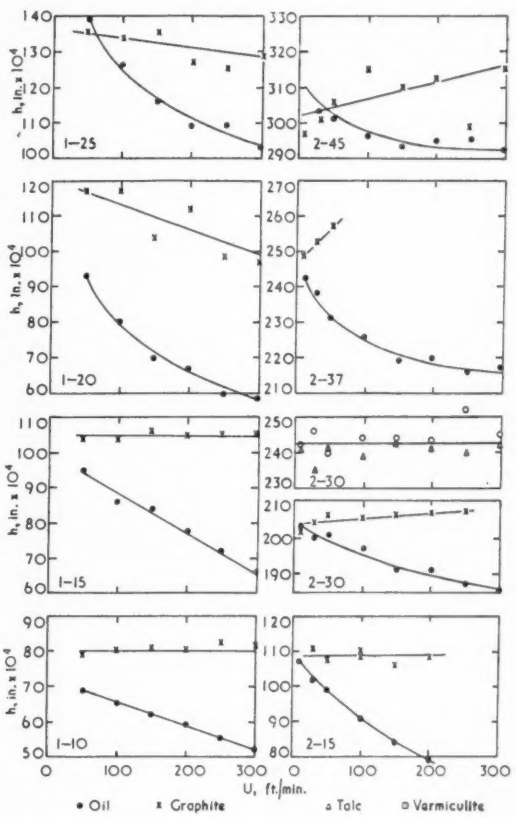


Fig. 5—Effect of speed on rolled strip thickness (copper with various lubricants)

Table II
COPPER RESULTS

Reference	Entry Thickness $H \times 10^4$, in.	Roll Speed, ft./min. U	Exit Thickness $h \times 10^4$, in.	Change of Exit Thickness from Lowest Speed $\Delta h \times 10^4$, in.	Specific Roll Load, tons/in. P	Coefficient of Friction $\bar{\mu}$
1*A*25a	240	50	139	...	14·0	0·080
b	241	102	126	−13	13·5	0·068
c	241	150	116	−23	13·0	0·062
d	240	195	109	−30	12·5	0·056
e	240	248	109	−30	12·5	0·054
f	241	290	103	−36	12·5	0·051
1*B*25a	239	50	135	...	14·5	0·075
b	240	104	134	− 1	14·5	0·077
c	239	150	135	0	14·5	0·075
d	236	198	127	− 8	14·5	0·072
e	237	247	125	−10	14·5	0·071
f	240	300	128	− 7	14·5	0·072
1*A*20a	190	50	93	...	19·5	0·075
b	190	100	80	−13	19·0	0·067
c	191	151	70	−23	18·5	0·062
d	192	198	67	−26	18·5	0·060
e	193	242	60	−33	18·5	0·058
f	194	290	59	−34	18·5	0·057
1*B*20a	195	54	117	...	21·5	0·096
b	195	103	117	0	22·0	0·096
c	196	157	104	−13	21·0	0·086
d	196	199	112	− 5	22·0	0·092
e	196	245	99	−18	21·5	0·084
f	197	293	97	−20	21·5	0·081
1*A*15a	140	50	95	...	16·0	0·080
b	140	104	86	− 9	15·5	0·071
c	141	152	84	−11	15·0	0·069
d	141	203	77	−18	15·0	0·063
e	141	250	72	−23	15·0	0·059
f	142	296	66	−29	14·5	0·054
1*B*15a	142	50	104	...	18·0	0·097
b	142	100	104	0	17·5	0·097
c	143	155	106	+ 2	17·5	0·097
d	143	200	105	+ 1	18·0	0·095
e	144	250	105	+ 1	18·0	0·095
f	145	298	105	+ 1	18·0	0·096
1*A*10a	90	50	69	...	16·0	0·072
b	91	101	65	− 4	16·0	0·067
c	92	152	62	− 7	16·0	0·062
d	93	203	59	−10	15·5	0·060
e	94	252	55	−14	15·5	0·055
f	95	307	52	−17	15·5	0·053
1*B*10a	96	50	79	...	16·5	0·084
b	97	101	80	+ 1	16·5	0·085
c	97	152	81	+ 2	17·0	0·090
d	98	207	80	+ 1	17·0	0·085
e	98	258	82	+ 3	17·0	0·091
f	98	310	81	+ 2	17·5	0·091
2*A*45a	460	9	310	...	10·1	0·078
b	461	31	303	− 7	9·9	0·074
c	452	53	301	− 9	9·8	0·074
d	456	103	296	−14	9·6	0·056
e	459	153	293	−17	9·4	0·049
f	460	194	295	−15	9·5	0·049
g	457	242	295	−15	9·4	0·049
h	456	295	292	−18	9·5	0·049
2*B*45a	454	8	297	...	11·7	0·124
b	459	28	301	+ 4	11·9	0·104
c	452	49	306	+ 9	11·6	0·105
d	458	98	315	+18	12·9	0·133
e	458	150	310	+13	13·1	0·128
f	460	203	312	+15	12·8	0·122
g	452	245	299	+ 2	12·9	0·110
h	455	298	315	+18	13·3	0·135

Table II—(*contd.*)

Reference	Entry Thickness $H \times 10^4$, in.	Roll Speed, ft./min. U	Exit Thickness $h \times 10^4$, in.	Change of Exit Thickness from Lowest Speed, $\Delta h \times 10^4$, in.	Specific Roll Load, tons/in. P	Coefficient of Friction μ
2*A*37a	371	8	242	...	11·2	0·080
b	370	28	238	− 4	11·1	0·076
c	371	51	231	−11	10·7	0·067
d	372	101	226	−16	10·3	0·064
e	365	150	219	−23	10·0	0·054
f	371	202	220	−22	10·1	0·054
g	369	245	216	−26	9·8	0·051
h	372	296	217	−25	9·7	0·048
2*B*37a	368	10	249	...	12·0	0·098
b	364	29	253	+ 4	12·3	0·107
c	366	50	257	+ 8	12·6	0·116
2*A*30a	295	10	204	...	10·8	0·081
b	299	28	200	− 4	10·8	0·076
c	310	53	201	− 3	10·8	0·072
d	311	104	197	− 7	10·3	0·072
e	305	151	191	−13	10·0	0·063
f	306	198	191	−13	9·4	0·049
g	309	243	187	−17	9·5	0·049
h	303	300	185	−19	9·5	0·049
2*B*30a	294	10	202	...	12·9	0·103
b	299	32	205	+ 3	12·7	0·101
c	293	53	207	+ 5	12·0	0·101
d	298	103	206	+ 4	12·5	0·101
e	289	152	207	+ 5	12·6	0·110
f	296	205	208	+ 6	12·6	0·111
g	297	248	208	+ 6	12·6	0·111
2*C*30a	300	8	241	...	10·5	0·126
b	287	27	235	− 6	10·3	0·132
c	296	50	241	0	10·7	0·135
d	289	100	239	− 2	10·6	0·143
e	294	152	243	+ 2	11·0	0·148
f	285	204	241	0	10·6	0·156
g	289	244	240	− 1	10·8	0·148
h	288	315	242	+ 1	10·8	0·156
2*D*30a	302	10	242	...	10·8	0·132
b	303	28	246	+ 4	10·8	0·137
c	284	50	240	− 2	10·8	0·156
d	293	100	244	+ 2	11·0	0·154
e	295	152	244	0	11·2	0·154
f	291	200	243	− 1	11·3	0·156
g	306	246	252	+10	11·6	0·157
h	294	310	245	+ 3	10·9	0·154
2*A*22a	234	10	140	...	13·8	0·080
b	214	33	131	− 9	13·2	0·076
c	225	52	126	−14	13·1	0·069
d	216	102	115	−25	12·6	0·065
e	221	151	106	−34	12·3	0·055
f	221	202	100	−40	12·0	0·051
g	216	243	95	−45	11·9	0·049
h	222	293	91	−49	11·8	0·045
2*A*15a	150	11	107	...	13·2	0·085
b	149	32	102	− 5	13·1	0·077
c	151	51	99	− 8	12·9	0·073
d	152	102	91	−16	12·6	0·066
e	152	151	84	−23	12·3	0·058
f	149	205	79	−28	11·8	0·054
2*B*15a	149	34	111	...	13·8	0·090
b	143	50	108	− 3	13·9	0·092
c	149	100	109	− 2	14·2	0·089
d	153	100	110	− 1	15·2	0·092
e	144	150	106	− 5	15·2	0·094
f	147	207	108	− 3	15·2	0·094

The Reference column contains a strip reference number, of which the first number indicates the group of experiments, the capital letter the lubricant (*A* rolling oil, *B* colloidal graphite, *C* colloidal talc, and *D* colloidal vermiculite). The group of two numbers indicates the nominal entry thickness, *i.e.*, 25 = 0·025 in.; and the final letter is the order within the group of strips.

Table III

CHANGES IN FRICTION DUE TO ROLL SURFACE

Reference	Roll Surface	Entry Thickness $H \times 10^4$, in.	Roll Speed, ft./min. U	Exit Thickness $h \times 10^4$, in.	Change of Exit Thickness from Lowest Speed $h \times 10^4$, in.	Specific Roll Load, tons/in. P	Coefficient of Friction, $\bar{\mu}$
3A37a	Mirror finish	377	8	268	...	8·3	0·056
b		368	53	257	−11	8·2	0·052
c		376	98	256	−12	8·1	0·042
d		369	203	250	−18	8·0	0·040
e		374	294	252	−16	8·0	0·039
2A37a	Mat surface	371	8	242	...	11·2	0·080
b		370	28	238	− 4	11·1	0·076
c		371	51	231	−11	10·7	0·067
d		372	101	226	−16	10·3	0·064
e		365	150	219	−23	10·0	0·054
f		371	202	220	−22	10·1	0·054
g		369	245	216	−26	9·8	0·051
h		372	296	217	−25	9·7	0·048

the experiments with the copper strip are shown in full in Table II.

The thickness of the steel coil rolled with lubricating oil decreased by approx. 0·008 in. as the mill accelerated. Only small irregular changes were found along the coil lubricated with graphite, until a small decrease in thickness occurred towards the end of the coil, probably due to contamination of the rolls with oil from the bearings. The thicknesses in each group of copper strips rolled with oil as lubricant decreased in every case with the rolling speed, whilst groups rolled with the graphite lubricant showed no systematic change. The group 2A45, for example, decreased in thickness by 0·0018 in. with a change in mill speed from 10 ft./min. to 300 ft./min., whilst the corresponding group rolled with graphite increased by a similar amount over the same speed range. In other groups rolled with graphite the thicknesses of the rolled strips showed little or no variation with speed. In one or two instances there was appreciable scatter in the graphite results, part of which may have been due to contamination of the graphite film with oil from the rolls.

In these experiments, both the deformed roll radius as calculated from Hitchcock's equation and the yield strength of the materials have been varied between wide limits; yet, in every case where two sets of nearly identical strips have been rolled, the speed effect has been eliminated and even altered in sign, simply by a change in the lubrication. The results with graphite as lubricant suggest that the yield stress of the material plays no part in the decrease in thickness with speed.

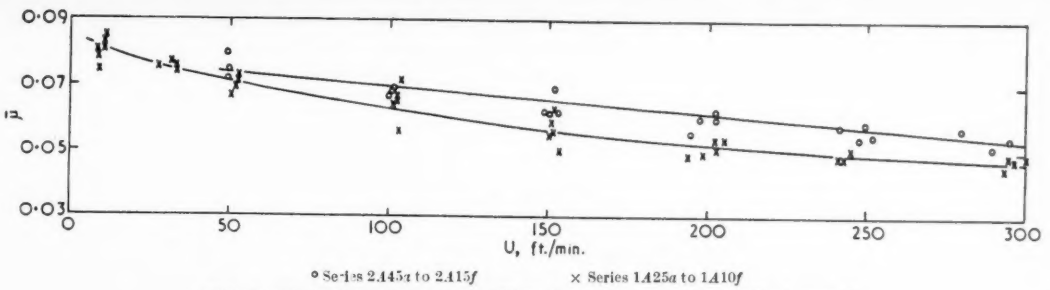
It appears, therefore, that the decrease in thickness that occurs with speed is caused by changes in strip friction, and the effect may be eliminated almost completely by the correct choice of lubricant. These conclusions have been confirmed by repeating two sets of experiments on copper strip, using colloidal talc and colloidal vermiculite as lubricants. The results (Table II) show that with both these lubricants there is a small increase in strip thickness with rolling speed.

The foregoing experiments were carried out using rolls with a mat surface finish of approximately ± 20 microinches measured axially along the roll face. An opportunity occurred subsequently to carry out a few experiments after the rolls had been reground to a mirror finish, the axial surface roughness being less than 4 microinches. A set of copper strips nominally 0·037 in. thick was used for this purpose (3A37), the strips being lubricated with rolling oil. The results are given in Table III, and for comparison the set 2A37 is reproduced from Table II. It will be seen that the speed effect measured as a variation in the rolled strip thickness is nearly halved, indicating that the speed effect is a function not only of the lubricant but also of roll finish.

VARIATION OF COEFFICIENT OF FRICTION WITH MILL SPEED

In previous experimental work, the changes either in the thickness of the rolled strip,[1,2] or in the mill load when rolling material of the same entry thickness[8] to a constant reduction have been taken as a measure of the speed effect. Both measurements suffer from the disadvantage that they relate the speed effect to the geometry of the mill and materials used in the experiments, whilst a quantitative measure is required which will enable the speed effect to be calculated for any mill for a given material. The change in the mean coefficient of friction between the rolls and the strip with speed has been adopted, therefore, as a measure of the speed effect, and experiments have been carried out to determine the form of the relationship between them.

The results of the experiments on copper strip (p. 291) have been used, together with an earlier series of experiments over a different range of entry thicknesses. The copper coils were rolled down from a thickness of 0·060 in. to lengths nominally 0·025, 0·020, 0·015, and 0·010 in. in thickness. Two groups of six strips of each thickness were rolled at speeds of 50, 100, 150, 200, 250, and 300 ft./min., one group being lubricated with rolling oil and the other with colloidal graphite. There are small but systematic



° Series 2A45a to 2A15f × Series 1A25a to 1A10f

Fig. 6—Variation of mean coefficient of friction with rolling speed

differences between the values of the coefficient of friction derived from the results of each of the two series of experiments. This may be because the experimental work on each was separated by an interval of six months, and the lubricants used, although they were of the same manufacture in each case, were of different batches, obtained just before the experiments were made. In this series of experiments on thinner strip, each loadmeter was supplied with A.C. at 500 cycles/sec. and the output proportional to the load was amplified and recorded on paper by a moving-pen milliammeter. The accuracy of this system of recording load was not within the 1% limits required, and for this reason the later experiments were carried out with a D.C. recording system, as described on p. 288. The results of these experiments are given elsewhere[1] (*loc. cit.*, Table II).

The measured values of load, mean yield, width of strip, and the draft, together with a value of the deformed roll radius calculated from Hitchcock's formula,[12] were substituted in equation (2), which was solved for the function $f(a,r)$. A value of μ was then obtained from the relationship $a = \mu\sqrt{(R'/h)}$, using Bland and Ford's values of $f(a,r)$ to solve for the parameter a. This value of μ is strictly a mean value of the coefficient of friction. It may be defined as the value of the ratio tangential-roll-pressure/normal-roll-pressure, which, treated as a constant and used with known values of the rolling variables of the pass in one or other of the theories of rolling, will give a pressure distribution producing the same total load as the actual distribution. Its value will be affected by errors due to the use of a mean yield strength instead of the true yield strength throughout the arc of contact, together with the errors due to the assumptions of plane deformation and constant curvature of the deformed rolls. No mathematical expression for the mean coefficient of friction in terms of the other rolling parameters will be attempted; it will be denoted here by $\bar{\mu}$, to distinguish it from the instantaneous value of the coefficient of friction at any point in the arc of contact. The values of $\bar{\mu}$ calculated from the experimental results are given in Table II.

The values of the mean coefficient of friction obtained from the results of experiments with oil as the rolling lubricant are shown in Fig. 6 as a function of the mill speed. Although the experiments were carried out over a wide range of rolling conditions, the scatter in the results is not large; a curve may be drawn through all the results of the series of experiments 2A45*a* to 2A15*f*, and another through the series 1A25*a* to 1A10*f*. No simple relationship exists between $\bar{\mu}$ and U. At mill speeds greater than about 100 ft./min., the curves correspond respectively to the relationships:

$$\bar{\mu}_A = 10^{-3}\exp[4\cdot90U^{-0\cdot038}]$$

and

$$\bar{\mu}_A = 10^{-3}\exp[5\cdot48U^{-0\cdot055}] \quad \text{.....(3)}$$

but at mill speeds below 50 ft./min. they appear to have the form:

$$\bar{\mu}_A = {}_S\bar{\mu}_A\exp(-\lambda U), \quad \text{.....(4)}$$

where λ is a constant and ${}_S\bar{\mu}_A$ is the static coefficient of friction of the rolls and strip. By extrapolating equation (4) the static coefficient of friction in both series of experiments was found to be 0·08.

Table IV
SLIP RESULTS

Reference	Calculation from Specific Roll Load $\bar{\mu}_A$	Calculation from Forward Slip $\bar{\mu}_A$
2A30a	0·081	0·074
b	0·076	0·070
c	0·072	0·070
d	0·072	0·069
e	0·063	0·065
f	0·049	0·054
g	0·049	0·062
h	0·049	0·052
2A45a	0·078	0·062
b	0·074	0·057
c	0·074	0·057
d	0·056	0·048
e	0·049	0·041
f	0·049	0·041
g	0·049	0·041
h	0·049	0·039
j	0·074	0·057
2A37a	0·080	0·068
b	0·076	0·063
c	0·067	0·053
d	0·064	0·055
e	0·054	0·044
f	0·054	0·045
g	0·051	0·042
h	0·048	0·036
2A22a	0·080	0·112
b	0·076	0·10
c	0·069	0·095
d	0·065	0·084
e	0·055	0·062
f	0·051	0·051
g	0·049	0·044
h	0·045	0·036

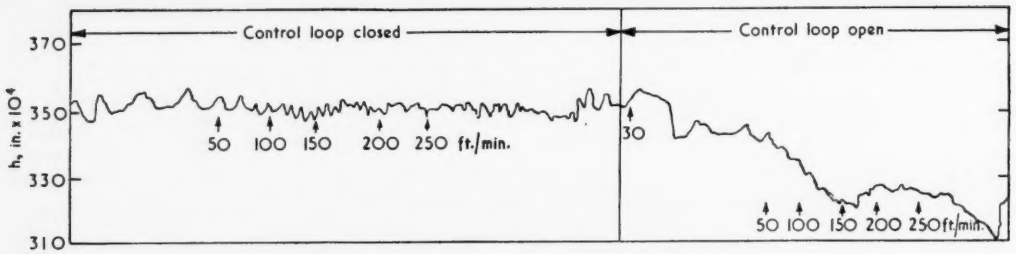


Fig. 7—Control of rolled strip thickness during acceleration

Extrapolation to mill speeds above or below the range covered by these experiments can only be tentative, and additional experimental work is required to establish the relationship over the entire range of mill speeds commonly used. The data of Fig. 6, however, show that the mean coefficient of friction is consistent for a given lubricant, roll surface finish, and strip material, over a wide range of rolling conditions. The mean coefficient of friction derived from the results of rolling copper strip between mirror-finished rolls is considerably lower than those found for the experiments with the mat surfaced rolls.

VARIATION OF FORWARD SLIP WITH SPEED

Values of the forward slip were obtained while rolling the short lengths of high-conductivity copper strip. They provide useful corroboration that friction is the speed-dependent variable in the rolling process.

An equation for the forward slip has been given by Sims,[14] and the measured values of the slip have been used in this equation to obtain values of the mean coefficient of friction. In Table IV these values are given against the corresponding values calculated from the rolling load over the range of $a = \bar{\mu}\sqrt{(R'/h)}$ for which the equations of forward slip are applicable. There is a close correlation between the two sets of values of $\bar{\mu}_A$. If the speed-dependent variable had been other than the strip friction, this agreement could have occurred only if $\bar{\mu}_A$, k, and R' were arranged symmetrically in the equations for load and slip. Hill[15] has shown that over a wide range of the parameters a and r,

$$F \simeq B\bar{k}\sqrt{(R'\delta)}[1{\cdot}081 + 1{\cdot}79ar\sqrt{(1-r)} - 1{\cdot}02r]$$

and the equation giving the forward slip is:

$$\tan^{-1}f = \tfrac{1}{2}\left[\tan^{-1}\sqrt{\left(\frac{r}{1-r}\right)} - \frac{1}{2a}\log_e\left(\frac{1}{1-r}\right)\right],$$

equations which are not symmetrical in these variables. There is strong support, therefore, for the hypothesis that friction is the speed-dependent variable.

CONTROL OF THE SPEED EFFECT

The principal cause of the speed effect in rolling is due to the changes in the strip lubricant, but it is known that hydrodynamically lubricated roll-neck bearings can contribute a secondary effect. Methods for controlling the speed effect to produce strip rolled to gauge during periods of acceleration and deceleration will be discussed under these headings.

Hydrodynamically Lubricated Bearings

Experiments have shown[6] that the changes in the oil film thickness are proportional to the mill speed, roll load, and the diametral bearing clearance. The changes in film thickness over a given range in mill speed may, therefore, be reduced only by decreasing the bearing clearance. Whether or not this is possible can be decided only after a study of each mill, and the relationship between bearing friction and the bearing modulus should be determined to discover if the oil film is likely to be ruptured at the threading speed of the mill.

Variations in the Coefficient of Strip Friction

The variations in the thickness of rolled strip due to variations in friction may be avoided by changing to a lubricant giving a coefficient of friction that does not vary with the mill speed. It has been shown that colloidal graphite and, to a lesser extent, talc and vermiculite possess this property over the speed range of the experimental mill. The objection to all three lubricants from the viewpoint of production is either that they are difficult to remove from the surface of the strip, or that they stain the surface or produce a poor surface finish. Moreover, in the form in which they are available at present, they are difficult to apply and may be expensive in comparison with the soluble oils, which have the additional advantage of acting as an effective roll coolant.

The speed effect due to changes in strip friction may be controlled by either of the two methods of gauge control proposed by Hessenberg and Sims,[3] and this has been demonstrated on an annealed mild-steel coil using the tension method of automatic control. In this method the strip tensions are varied to keep the roll force constant in a pass where the roll setting S_0 is kept constant. If the elastic constants of the mill do not depend on the mill speed, there is a unique relationship between the rolled strip thickness and the mill load for any setting of the rolls; hence, in a pass of constant mill load the strip will be rolled to a uniform gauge along its length, regardless of changes in strip width, hardness, or frictional conditions.

The variation in thickness along the coil after rolling from 0·063 in. to 0·035 in. thick is shown in Fig. 7. The automatic control[13] was connected into the mill drive and the mill accelerated from a speed of 30 ft./min. to a maximum speed of about 270 ft./min. and then decelerated again to 30 ft./min. Without stopping the mill, the experiment was repeated without the automatic control in the mill drive circuit. The periodic fluctuations shown in Fig. 7 are due to roll eccentricity. With gauge control the strip thickness was held to 0·0352 in., $\pm$ 0·0003 in. if the changes in thickness due to roll eccentricity are ignored; without gauge control a decrease in thickness of 0·004 in.

occurred as the mill accelerated. The slight decrease in thickness at a mill speed of 150 ft./min. when rolling with automatic control was due to the saturation of the booster field as the Ward-Leonard speed control of the drive changed over from increasing generator field to weakening the mill motor field, resulting in a temporary loss of gain in the closed loop system.

It is of interest to note that in the pass with automatic control of gauge the rolled strip thickness has been held to within $\pm 1\%$. From equation (1) it will be seen that the value of the elastic coefficient M of the mill must have remained within the same limits, since, with a fixed roll setting and the mill load held nearly constant, a change in the value of M would have produced an equal and opposite change in h.

CONCLUSIONS

An experiment has been described which enables the speed-dependent variables of the rolling process to be determined. The results indicate that it is the coefficient of friction between the rolls and the strip which changes, and not the elastic parameters of the rolls or the yield strength of the material of the strip.

A measure of the change in the mean coefficient of friction with mill speed has been obtained for a rolling oil and high-conductivity copper, but the experimental results are not sufficiently complete to indicate the reason for the dependence of friction on speed. The coefficient of friction with boundary conditions of lubrication may be influenced by temperature, pressure, and the relative velocities between the rubbing surfaces. Bowden and Tabor[16] state that the rubbing friction between metal surfaces does not, in general, vary continuously with temperature. In the results of the present experiments there is no indication that the coefficient of friction depends markedly on strip temperature which at any given rolling speed will increase with the parameter* $a = \bar{\mu}\sqrt{(R'/h)}$; moreover, there is no correlation between $\bar{\mu}$ and the specific roll pressure.† It is suggested, therefore, that variation in the rubbing speed between the rolls and the strip is the principal cause of the changes in friction and of the magnitude of the coefficient of friction, and its change with speed depends upon the surface finish of the rolls. The rolling theories are not sufficiently precise, and the mathematics of the process are too complex for a relationship between the true coefficient of friction and rubbing velocity to be obtained from the experimental results, or to permit an extrapolation to determine the coefficient of friction at mill speeds outside the range of the experimental mill.

Further experimental work is being carried out to determine the relationship between the true coefficient of friction and the rubbing velocity, and from the results it is hoped that the relationship between the mean and true coefficients can be derived. Meanwhile, the present experiments indicate the possible methods of preventing the strip thickness changing with speed. Either a rolling oil must be found which does not change in lubricity with speed, or the mill must be fitted with a closed loop system for automatic gauge control. A possible lubricant is colloidal graphite, but in cold rolling it has the disadvantages of a relatively high coefficient of friction and is, therefore, productive of high rolling loads*; roll cooling becomes difficult and the residual graphite can only be removed with difficulty from the surface of the rolled strip. Moreover, it does not produce the high lustre essential in many applications of cold rolled material. If the strip is to be used subsequently for deep drawing, however, the presence of surface graphite may be an advantage. No compound has yet been found which will produce a constant, relatively small coefficient of friction and yet will not stain the strip in subsequent annealing.

Automatic gauge control systems will provide a complete correction of the speed effect if they have a sufficiently fast response. By fitting high-performance closed loop controls to the mill, a practical solution to the speed effect problem may be found.

Acknowledgments

Grateful acknowledgment is made to Messrs. Acheson Colloids, Ltd., London, and to I.C.I. (Metals), Ltd., Wilton, for the supply of materials.

* There is a close correlation between $\bar{\mu}$ and reduction in any of the experimental sets made at constant entry thickness. For instance, in the set 2A22, $\bar{\mu}$ decreases with increasing reduction. This is because in passes where the screw setting is held constant the reduction is a function of mill load and hence of $\bar{\mu}$, a relationship which has been discussed by Hessenberg and Sims.[3]

† In rolling, the specific roll pressure is defined as the ratio of the mill load to the area of the arc of contact, and may be calculated from the formula $p = F/B\sqrt{(R'\delta)}$.

* The values of $\bar{\mu}_B$ given in Table II for the graphite experiments are representative of the values commonly found for this lubricant in the dry condition.

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Studies in the Deoxidation of Iron

DEOXIDATION BY SILICON

By E. Ll. Evans, B.Sc., and H. A. Sloman, M.A., F.R.I.C., F.I.M.

SYNOPSIS

This paper describes the preparation, microscopical examination, and analysis of a series of ingots made by adding various quantities of iron oxide and silicon to 300–400-g. melts of pure iron, *in vacuo*. Results reported include the X-ray and chemical analysis of the non-metallic residues extracted from ingot samples by alcoholic iodine solution, vacuum fusion analyses of total oxygen contents, and chemical analyses of total silicon contents. Where the silicon added is stoichiometrically in excess of the oxygen, the inclusions found are vitreous.

At silicon contents of the iron ingots exceeding 0·1 %, the inclusions are composed of practically pure silica. At lower silicon contents the iron oxide content of the inclusions progressively increases. Inclusions in this range may consist of two immiscible glasses, or of fayalite together with magnetite and wüstite. Photomicrographs are shown of the various types of inclusion. The results are discussed briefly in relation to Gokcen and Chipman's recent investigation of the equilibrium in molten iron.

THE present paper reports a continuation of an investigation of the products of deoxidation of iron by various elements, in which attention is given to the nature and mechanism of formation of the deoxidation product.

The methods employed for the production, analysis, and examination of samples of iron deoxidized by silicon were similar to those described in earlier reports on deoxidation by aluminium[1] and manganese.[2]

Paper SM/BB/81/52 of the Gases and Non-Metallics Sub-Committee of the Ingots Committee of the Steelmaking Division of the British Iron and Steel Research Association, received 31st July, 1952. The views expressed are the authors' and are not necessarily endorsed by the Sub-Committee as a body.

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PREPARATION AND SAMPLING OF INGOTS

The ingots used in the investigation were prepared by melting charges of 300–400 g. of NPL Mark 2 iron[3] in sintered alumina crucibles in an evacuated high-frequency induction furnace.[1] An initial addition of oxygen was made to each melt by placing a weighed quantity of ferric oxide in a plugged hole at the base of the iron charge. Granular silicon of high purity ('insoluble' content 0·032 wt.-%, Al 0·025 wt.-%, Ca 0·045 wt.-%) contained in a small capsule of iron

Table I

ANALYSIS OF INGOTS OF IRON DEOXIDIZED BY SILICON

Ingot	Chemical Analysis of Alcoholic Iodine Residues, wt.-% of Ingot Sample						Total Oxygen, wt.-%		Total Si Content, wt.-%	Si as SiO_2, wt.-% (2)−(3)	Si in Metallic Solution, wt.-% (10)−(11)
	SiO_2		FeO		Al_2O_3		Alcoholic Iodine Residue (3)+(5)+(7)	Vacuum Fusion			
	Oxide	Oxygen	Oxide	Oxygen	Oxide	Oxygen					
(1)	(2)	(3)	(4)	(5)	(6)	(7)	(8)	(9)	(10)	(11)	(12)
SA	0·0028	0·0015	0·056	0·012	0·0021	0·0010	0·015	0·030	0·0039	0·0013	0·0026
SB	0·0052	0·0027	0·022	0·005	0·005	0·0023	0·010	0·019	0·008	0·0025	0·0055
SC Top	0·0206	0·011	0·406	0·090	0·0014	0·0006	0·102	0·119	0·017	0·010	0·007
SC Bottom	0·0176	0·009	0·415	0·092	0·0019	0·0009	0·103			0·008	0·009
SD Top	0·0185	0·010	0·34	0·075	0·0014	0·0006	0·086	0·101	0·021	0·009	0·012
SD Bottom	0·020	0·0105	0·335	0·074	0·0013	0·0006	0·085			0·009	0·012
SE	0·048	0·026	0·105	0·023	n.d.	...	0·049	0·062	0·034	0·022	0·012
SF	0·0119	0·0063	0·004	0·0009	0·0037	0·0017	0·009	0·008	0·020	0·0056	0·014
SG	0·071	0·038	0·072	0·016	0·0026	0·0012	0·055	0·066	0·050	0·033	0·017
SH	0·058	0·031	0·011	0·002	n.d.	...	0·033	0·043	0·047	0·027	0·020
SJ	0·072	0·038	0·0061	0·0014	0·0025	0·0012	0·041	0·043	0·09	0·034	0·06
SK	0·033	0·017	0·0036	0·0008	0·0017	0·0008	0·019	0·019	0·081	0·016	0·065
SL	0·006	0·003	0·003	0·001	0·005	0·002	0·006	0·006	0·076	0·003	0·073
SM	0·031	0·017	0·0028	0·0006	n.d.	...	0·017	0·028	0·10	0·015	0·085
SN	0·047	0·025	0·0032	0·0007	0·0013	0·0006	0·026	0·011	0·286	0·022	0·26
SP Top	0·048	0·026	0·0029	0·0006	0·0040	0·0019	0·028	0·020	0·37	0·023	0·35
SP Bottom	0·043	0·023	0·0061	0·0013	0·0031	0·0014	0·025			0·020	0·35
SQ	0·0162	0·0086	0·0038	0·0008	0·0057	0·0027	0·012	0·008	0·36	0·008	0·35
SR	0·0023	0·0012	0·0011	0·0002	0·0025	0·0012	0·0026	0·0033	0·38	0·001	0·38
SS Top	0·0056	0·0030	0·0041	0·0009	0·0025	0·0012	0·005	0·005	0·64	0·0026	0·64
SS Bottom	0·0085	0·0045	0·0045	0·0010	0·0051	0·0024	0·008			0·004	0·64
ST	0·028	0·015	0·0038	0·0008	0·0053	0·0025	0·018	0·020	0·86	0·013	0·85

n.d. = not determined

sheet was added, by means of a magnet, from a side-arm in the furnace superstructure shortly after the iron charge became molten.

The temperature of the melt was in general not permitted to rise above 1600° C. After allowing sufficient time for the silicon addition to become thoroughly mixed with the iron, the furnace current was switched off and the alloy was solidified and cooled *in vacuo*. In the preparation of ingots *SA*, *SB*, *SF*, *SK*, and *SN*, the method of cooling was modified in a manner described later.

A microscopical examination was made of a complete longitudinal half-section of one of the ingots in which large initial additions of oxygen and silicon had been made, to determine the segregation of inclusions. Whilst the inclusions were not uniformly distributed, the only gross segregation found was confined to a layer about 1 mm. in thickness nearest the top surface of the ingot. In taking samples for analysis, therefore, the top 4 mm. of the ingot were rejected; the remainder of the surface was cleaned of any adhering refractory or oxide coating by filing heavily.

The filed ingots were quartered longitudinally, and samples were taken as follows:

(*a*) Cylindrical pieces for vacuum fusion analysis (from a central position)

(*b*) Slices 1 mm. thick for alcoholic iodine extraction (one 10-g. sample for X-ray examination, and one or two 10-g. samples, taken either from the centre or from the top and bottom, for chemical analysis of the residue)

(*c*) Millings for chemical analysis of silicon (from the complete longitudinal section).

ANALYSIS OF THE INGOTS

Alcoholic iodine extractions of the ingots for chemical analysis of the residues were carried out in the normal way at 65° C. The filtered residues were ignited, and the oxides were determined gravimetrically.

All residues for X-ray examination by the Debye-Scherrer powder method were extracted at 30° C., this lower temperature being employed with the intention of retaining a proportion of any extraneous compounds other than oxides (*e.g.*, sulphides) which may have been present in the inclusions.

The total oxygen contents of the ingots were obtained by the vacuum fusion method, and the total silicon contents by acid solution followed by colorimetric determination.

ANALYTICAL RESULTS

The results of the various analyses are summarized in Table I. In addition to the vacuum fusion analyses, total oxygen contents have been obtained from a summation of the oxygen present as oxides in the alcoholic iodine residues. Comparison between the two sets of figures may suggest at first that agreement is not good. However, with one exception (ingot *SN*), the discrepancy does not exceed a factor of 1: 2, and this variation may be explained by the segregation of inclusions. This segregation results from the method used in making the ingots, which was deliberately aimed at the retention in the body of the solidified metal of a large proportion of the initial deoxidation product. Duplicate alcoholic iodine analyses were carried out on ingots *SC*, *SD*, *SP*, and *SS*, and the discrepancy in the results for ingot *SS* illustrates the degree of segregation which can occur. The extent to which agreement is found, despite the segregation, and the occurrence of this agreement over a wide range of inclusion compositions, suggests that there is no systematic disagreement between the vacuum fusion and the alcoholic iodine residue analyses of any of the types of inclusion encountered as, for example, in the analysis of pure or almost pure MnO.

In column 12 of Table I, values are given for the metallic silicon contents of the ingots. These were derived by subtracting the silicon content of the alcoholic iodine residues from the total silicon contents of the ingots. Since these amounts were determined on separate samples, the values in column 12

Table II

RESULTS OF X-RAY ANALYSES OF RESIDUES FROM INGOTS OF IRON DEOXIDIZED BY SILICON

Residues from ingots omitted from this Table were either too small or gave no distinct X-ray diffraction pattern

Ingot	Wüstite FeO	Magnetite Fe_3O_4	Fayalite Fe_2SiO_4	*X**	α-Tridymite SiO_2	Lines Corresponding to Other Compounds
SA	Large	Medium	...	...	...	...
SB	Medium	Small	...	...	...	About 50% of lines unidentified
SC	Large	Medium	Medium	...	...	2 lines, spacings 3·18 Å.† and 1·075 Å.
SD	Large	Small	Med. large	...	...	Weak line of spacing 1·23 Å.
SE	Medium	Trace	...	Small	Medium	9 medium, 3 weak unidentified lines
SG	Trace	...	...	...	...	...
SH	...	Medium	...	Trace	...	Medium quantity iron sulphide, 4 medium, 7 weak unidentified lines
SL	...	Large	Medium	Medium	Trace	7 medium, 11 weak unidentified lines, some of very high spacings
SM	...	Medium	...	Small	Medium	Trace of iron silicide FeSi; 7 medium, and 6 weak unidentified lines
SP	...	...	...	...	...	Numerous weak–medium lines; small quantity cyanite ($Al_2O_3.SiO_2$); trace ? mullite ($3Al_2O_3.2SiO_2$)
SS	...	...	? Trace	...	...	Some faint lines; trace ? cyanite

* Substance *X* characterized by spacings of about 3·4 Å. and 3·2 Å.
† This corresponds to the most intense line of cyanite, $Al_2O_3.SiO_2$

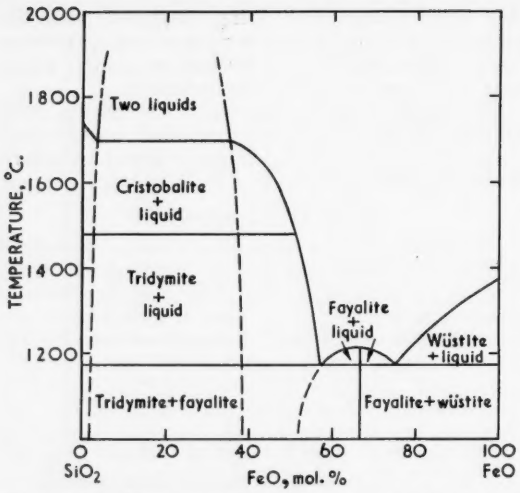

Fig. 1—Phase diagram of SiO_2–FeO system, after Bowen and Schairer, showing probable form of miscibility gap in vitreous state

are subject to a dual error due to segregation. The ingots are lettered and tabulated in the order of increasing metallic silicon content.

Melts *SE*, *SH*, *SL*, *SR*, and *SM* were carried out in porous alumina crucibles; the remainder were prepared in crucibles of sintered alumina. The alumina contents of these later melts are, however, higher than those found in the earlier work on ingots deoxidized by manganese. In some of the ingots the proportion of alumina to other constituents in the inclusions is sufficiently great to influence materially the compositions of the inclusions, and this was taken into account when inferring the nature of the inclusions formed by pure three-component melts.

The results of the X-ray examination of the alcoholic iodine residues by the Debye-Scherrer powder method are given in Table II. The outstanding feature of the system under consideration is that the main deoxidation product, silica, or a silica-rich FeO–SiO_2 phase, occurs in the ingots in vitreous form and the information obtained by X-ray techniques is therefore limited. The residues omitted from Table II contained too small an amount or proportion of crystalline material to give a recognizable Debye-Scherrer pattern.

A large proportion of identifiable material was found mainly in those residues in which the ratio of iron oxide to silica lies between those of fayalite (Fe_2SiO_4) and pure iron oxide. Among such residues (*SB*, *SC*, and *SD*) iron oxide is present, together with fayalite in *SC* and *SD*. Ingot *SL* contains fayalite and iron oxide, although the chemical analysis of the residue shows an excess of silica to be present. This ingot will be mentioned again later. Iron oxide was identified in these and some other residues in the form of wüstite (FeO), magnetite (Fe_3O_4), or both. This variable composition of the iron oxide is a source of error in the alcoholic iodine analysis of total oxygen, in which it is generally assumed that the only iron oxide present is FeO.

Crystalline silica was detected in residues from ingots *SE*, *SL*, and *SM*, in the form of α-tridymite. These ingots had been furnace-cooled, the H.F. supply being switched off completely after the addition of silicon to the melt. In an attempt to induce further crystallization, ingots *SA*, *SB*, *SF*, *SK*, and *SN*, which were prepared later, were kept at a temperature between 1000° and 1200° C. for at least 2 hr. after solidification. This treatment failed to produce detectable amounts of tridymite; it seems probable, therefore, that the crystallization of α-tridymite in the earlier melts was not directly related to rate of cooling, at least in the solid ingot.

Only the roughest indication could be obtained from the X-ray evidence of the proportion of crystalline material in the residue. Obviously this proportion was small where a generous sample of residue gave only faint lines after a lengthy exposure in the powder camera. This conclusion applies to the FeSi in ingot *SM*, FeS in ingot *SH*, and cyanite ($Al_2O_3.SiO_2$) and possibly mullite ($3Al_2O_3.2SiO_2$) in ingot *SP*. With the exception of the traces of aluminium silicates in ingots *SP* and *SS*, no crystalline compounds of aluminium were identified throughout the whole series.

NATURE OF THE DEOXIDATION PRODUCTS

Before dealing with the microscopical appearance of typical inclusions, it will be convenient to discuss the nature of the slag phase with which the deoxidized melts are in equilibrium. An iron melt containing an excess of dissolved silicon will be in equilibrium with silica. When there is no silicon in the iron, the slag phase is liquid ferrous oxide. At very low silicon contents, therefore, the equilibrium slag phase is intermediate between FeO and SiO_2. The range of intermediate slags, which are equivalent to deoxidation products, is shown by reference to the phase diagram (Fig. 1) derived by Bowen and Schairer[4] for the FeO–SiO_2 system, for the most part in equilibrium with iron, and is confirmed generally by the analytical and X-ray results described in the previous section.

As the metallic silicon content of the metal increases from zero, the slag in equilibrium with the molten iron alloy changes continuously from pure FeO to a FeO–SiO_2 solution intermediate in composition between fayalite (Fe_2SiO_4) and silica. On cooling, a slag or inclusion within this composition range will, if not too rich in silica (in which case it will remain vitreous), crystallize to give wüstite or fayalite and a fayalite–wüstite eutectic, or possibly fayalite with a fayalite–tridymite eutectic. With iron melts of a higher range of silicon contents, the equilibrium slag will consist of a mixture of crystalline silica and a liquid phase, or, more probably (since silica tends to remain vitreous), two immiscible liquids. Where silicon is in sufficient excess in the melt, the equilibrium deoxidation product should be practically pure silica. In Fig. 1 the liquid miscibility gap and the fayalite liquidus are extrapolated to show the form of the boundaries in the vitreous state.

The foregoing consideration is greatly simplified since it takes into account a pseudo-binary slag system, whereas in fact the system being considered is ternary. This accounts for the presence of magnetite (Fe_3O_4)

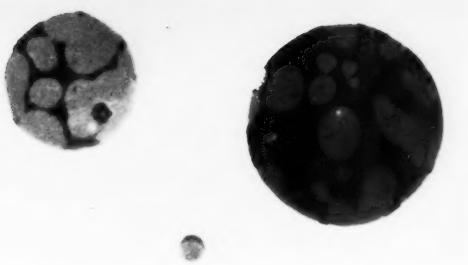

Fig. 2—Wüstite+fayalite —Wüstite eutectic

Fig. 3—Wüstite+fayalite (degenerated eutectic ?)

(Note—In Fig. 2 evidence of the eutectic has been lost in reproduction)

Fig. 4

Fig. 5

Figs. 4 and 5—Two immiscible glasses

Fig. 6— Immiscible glasses and end-members of immiscibility gap

Fig. 7—Colourless vitreous silica

Figs. 2—7 : × 1000

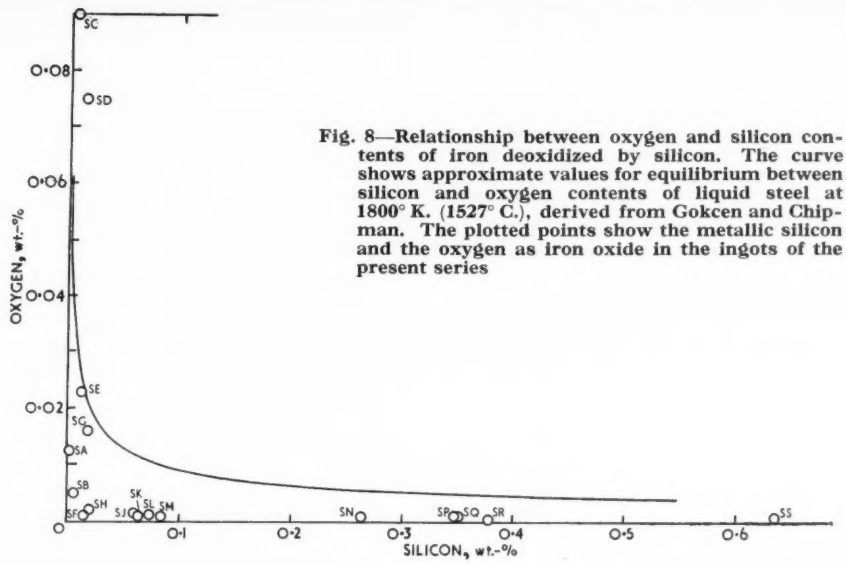

Fig. 8—Relationship between oxygen and silicon contents of iron deoxidized by silicon. The curve shows approximate values for equilibrium between silicon and oxygen contents of liquid steel at 1800° K. (1527° C.), derived from Gokcen and Chipman. The plotted points show the metallic silicon and the oxygen as iron oxide in the ingots of the present series

in quite large proportions, relative to the wüstite, in a number of the residues.

MICROSCOPICAL EXAMINATION

Polished sections of all ingots were examined microscopically. Visual examination was carried out with illumination from a tungsten-filament lamp with a Chance 0*B*9 'daylight correction' filter. Photomicrographs were taken using green monochromatic illumination from a mercury arc. The range of inclusions postulated in the previous section was in fact observed in the ingots examined. Some of the main types of inclusion were found as the principal deoxidation product in one or other of the ingots, but by a fortuitous circumstance, the whole series was found in one polished section taken from the top of ingot *SL* and parallel to its cylindrical axis. The iron oxide appeared to have floated to the top after partial melting of the iron charge and was still in the course of diffusing into the iron when, after addition of silicon, the melt was allowed to solidify. The inclusions range from those rich in FeO near the surface to practically pure SiO_2 in the body of the ingot. This provided a useful though unexpected confirmation of the full range of possible types of inclusion.

Figure 2 shows an inclusion in ingot *SD* containing globules which appear to consist of wüstite in a dark grey matrix in which a fine eutectic structure may be seen. Some of the wüstite globules in this inclusion are edged with a paler grey phase which may be derived from one of the trace impurities in the iron. Figure 3 shows another typical (though large) inclusion also from ingot *SD*, having a higher proportion of the dark grey constituent which, however, contains no observable eutectic structure. It seems reasonable to assume that the dark grey constituent is fayalite, especially since it remains unattacked when the iron oxide is dissolved in 5 min. by 5% alcoholic hydrochloric acid, and is itself removed by a 10-min. etch with 20% aqueous hydrochloric acid. No microscopical evidence was obtained of the presence of magnetite (Fe_3O_4). It may be dispersed within the fayalite or it may exist as sub-microscopic particles within the pale grey wüstite. The latter suggestion is unlikely, for although it is possible to obtain sharp X-ray diffraction lines from crystals which cannot be distinguished under the ordinary microscope, such fine particles would probably be present over a wide range of size. No diffuseness of the X-ray pattern was observed. It is also possible that the magnetite occurs in the rough-surfaced portions observed in a number of inclusions and shown in Fig. 3. For the present, it seems that the explanation of the presence of magnetite must remain incomplete, as in the work on deoxidation products of aluminium.[1] The authors have not found magnetite in virtually pure-iron/oxygen ingots prepared similarly to those described in this report.

With increasing silica contents, the inclusions remained spherical and passed first into an apparently homogeneous dark grey opaque fayalite, then into a dark vitreous form with green transmission. The inclusions within this range became increasingly transparent with increasing silica.

A large proportion of nearly spherical inclusions appearing to consist of two immiscible glasses* was found in ingot *SG* (Figs. 4 and 5). The general appearance of these duplex glasses is similar to that recorded by Greig[5] in a study of the immiscibility gap in silica/metal-oxide systems. In the same ingot there also appeared single-phased glassy inclusions of a variety of shades of green and tending to be not

* Under crossed polarizing filters, globular vitreous inclusions appear bright with a black cross and concentric rings. The cross remains fixed relative to the optical system of the microscope when the specimen is rotated (*see, e.g.*, S. L. Hoyt and M. A. Scheil, *Trans. Amer. Inst. Min. Met. Eng.*, 1935, vol. 116, p. 405). In the duplex inclusions the cross is only slightly deformed as it passes the interface of the two phases.

completely circular in section (Fig. 6). These were probably end-members of the two-phase liquid region of the oxides. It could not be determined whether the silica-rich phase was entirely colourless, since the overall green coloration of the duplex inclusions might or might not be produced by internal reflection of green transmitted light from one of the phases. Some inclusions of this type were found in ingot *SL*.

In these ingots, which contained an appreciable excess of metallic silicon, the inclusions consisted of very highly transparent, colourless, spherical globules of silica (Fig. 7).

In selecting the typical inclusions for illustration, those ingots have been chosen which have a relatively low alumina content, to ensure that the inclusions contain substantially only the three main components, iron, silicon, and oxygen.

Zappfe and Sims[6] have reported the occurrence, in ingots quenched from the melt, of glassy silica inclusions surrounded by smaller ' satellite ' inclusions which appear to have been halted in the course of agglomerating to form a single globule. A few inclusions of this type were observed near the top of ingot *SR*, but they cannot be regarded as typical.

DISCUSSION

The results of a careful study of the silicon–oxygen equilibrium in liquid iron have been published recently by Gokcen and Chipman.[7] Values which they obtained for the ' equilibrium constant ' [% Si] [% O]^2 for equilibrium between melt and solid silica have been extrapolated graphically to a temperature of 1800° K. (1527° C.) and used to construct the curve in Fig. 8. As Gokcen and Chipman have pointed out, this curve is only approximately true owing to the variation of the activity coefficients of the silicon and oxygen with changes in their concentrations. It is, however, sufficiently accurate to illustrate the desirability of regarding the deoxidation reaction in practice as consisting of a series of consecutive processes. In addition to the curve of equilibrium in liquid iron at approximately its melting point, Fig. 8 shows the analyses of metallic silicon and of oxygen as FeO in the ingots, taken from Table I. In the presence of excess silicon a large proportion of the oxygen in solution in the liquid steel reacts to form silica on solidification and cooling of the iron ingot. The first stage of the deoxidation reaction consists of the establishment of equilibrium in the liquid iron. Given sufficient time, the precipitated deoxidation product floats to the surface of the melt. On solidification, a shift occurs in the equilibrium, and further deoxidation product is precipitated. The completeness of removal of this fraction will depend on various factors, such as mould design and mode of solidification. A further change in the equilibrium relationship on cooling the solid ingot will result in further precipitation of silica, which, of course, must remain in the ingot. This final fraction of silica is probably small in comparison with the quantity precipitated on solidification of the iron.

Theoretically, it should be possible to calculate an envelope extending tangentially from Gokcen and Chipman's curve to the ordinate of solubility of oxygen in pure iron. Such a curve would indicate the composition of the slag in equilibrium with iron of various high oxygen contents or low silicon contents. In practice, it is not possible to obtain any useful curve from available data, even for equilibrium with a liquid slag of fayalite composition. Results of the present work show that the deoxidation product departs increasingly from a substantially pure silica at residual silicon contents of the steel below 0·08 wt.-%, and that equilibrium of liquid iron with a slag of composition corresponding to fayalite occurs at a dissolved oxygen content greater than 0·02 wt.-%. (In this connection it is reasonable to ignore the anomalous position of ingot *SL*.) By both criteria, of oxygen and silicon contents, a steel which in practice would be regarded as deoxidized would be in equilibrium only with practically pure silica.

CONCLUSIONS

The principal product of deoxidation of iron by silicon, when silicon is in stoichiometric excess of oxygen, is practically pure silica.

As the residual silicon content of the iron decreases below 0·1%, the slag phase with which the iron is in equilibrium increases in iron oxide content. Deoxidation products identified in ingots within this composition range include two mutually insoluble glasses, and fayalite, which generally occurs in the solidified iron together with the iron oxides wüstite and magnetite. The order of formation of these inclusions can be explained by existing knowledge of the Fe–O–Si system, although the part played by magnetite is not clear.

No difficulties have arisen in the application of the alcoholic iodine extraction method for the isolation of the inclusions formed in iron by deoxidation with silicon, although the presence of magnetite in addition to wüstite introduces a small error in the determination of oxygen as iron oxide.

It has been shown that a large proportion of the oxygen remaining in solution in liquid iron in the presence of a moderate excess of silicon is converted to silica on or after the solidification of the iron.

Acknowledgments

The work described in this paper was carried out in the Metallurgy Division of the National Physical Laboratory on behalf of the Gases and Non-Metallics Sub-Committee of the British Iron and Steel Research Association, and the paper is published by permission of the Director of the Laboratory with the approval of the British Iron and Steel Research Association.

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Effect of Quench Ageing on Strain Ageing in Iron

By A. H. Cottrell and G. M. Leak

SYNOPSIS

The dislocation theory predicts that the rate of strain ageing in iron should be proportional to the concentration of carbon and nitrogen in solution, provided that an allowance is made for their different diffusion coefficients. This concentration can be varied by means of quench-ageing treatments given before the material is strain aged. These effects are examined experimentally from stress/strain measurements on soft iron wire. The limit of proportionality is a more sensitive index of strain ageing than the yield stress. As measured from the limit of proportionality, freshly quenched specimens strain age at about five times the speed of those quench aged at 120–300° C., and this difference is explained quantitatively. A theoretical estimate of the absolute solute concentration to give a certain rate of strain ageing is shown to be correct in order of magnitude. Specimens quench aged at 63° and 45° C. strain age too quickly to be accounted for by the equilibrium solubilities of carbon and nitrogen at these temperatures. An explanation is offered in terms of the small particle size of these precipitates, which increases the solubility. Because of its lower solubility and lower diffusion rate, carbon causes insignificant strain ageing in quench-aged material, compared with nitrogen. Only in the freshly quenched material is carbon more important than nitrogen.

Introduction

THIS investigation started from the following ideas:

(i) That quench ageing in soft steel is caused by the precipitation of carbon and nitrogen as carbide and nitride particles from supersaturated solution in ferrite

(ii) That strain ageing in soft steel is caused by the segregation of carbon and nitrogen atoms to dislocations either freed or created by plastic deformation

(iii) That the energy binding a carbon or nitrogen atom in a dislocation is greater than that binding it in a carbide or nitride particle

(iv) That the amount of carbon or nitrogen that can be absorbed by the dislocations in a fully worked specimen is often greater than that left in solution after quench ageing.

The experimental observations[1–7] and theoretical deductions[8–9] leading to the first two ideas are now well known, and will not be further described in detail here. Although lattice distortions, other than dislocations, might give resultant interaction with carbon and nitrogen atoms, and so might take part in strain ageing, this aspect has not yet proved amenable to quantitative treatment. Experimental work[14, 15] has already shown results that agree well with predictions based on the quantitative treatment of interaction between carbon and nitrogen atoms and edge dislocations.

When a low-carbon steel is quenched from about 700° C., and aged at 200–300° C., fine particles of carbide become visible in the microstructure. Andrew and Trent[7] showed that these do not appear near a hardness indentation made after quenching and before ageing, which suggests that in this cold-worked region the carbon atoms prefer some distribution other than that of visible precipitates. Later experiments were made by Dijkstra,[10] who applied the Snoek damping method[11] to determine the amount of nitrogen retained in interstitial solution in ferrite. Using wires that were either quenched and aged, or quenched and then strained and aged, he found that the solubility of nitrogen in the presence of nitride particles was 0·012 wt.-% at 300° C. and 0·025 wt.-% at 400° C., whereas the solubility in a strained specimen (*i.e.*, the solubility in the presence of dislocations) was only 0·0025% at 300° C. and 0·006% at 400° C. Moreover, although more nitrogen went from solution in the strained specimen, no precipitated particles could be seen. This is evidence that the nitrogen atoms are more firmly bound in dislocations than in nitride particles. Theoretical estimates of the binding energy of carbon and nitrogen atoms in dislocations in iron lead to the same conclusion.[12]

There is evidence[13] that in heavily worked metals the density of dislocation lines approaches a limiting value of 10^{12} per sq. cm. In a moderately worked specimen, such as will be considered in this paper, 10^{11} per sq. cm. is a reasonable estimate. To anchor these dislocations sufficiently well to cause a full return of the yield point, about 0·001% of solute is required,[9] and the experimental work of Cottrell and Churchman[14] and Harper[15] suggests that amounts of carbon and nitrogen of this order are, in fact, absorbed during strain ageing. However, the equilibrium solubility of carbon in ferrite at room temperature, as deduced by extrapolating from higher temperatures, is only about 10^{-7}%.[16, 17] The solubility of nitrogen is somewhat higher, about 10^{-5} or 10^{-4}% at room temperature.[17, 18] How, then, do the dislocations obtain all the solute they need?

Presumably, either the metal is supersaturated with carbon or nitrogen, or dislocations make carbide and nitride particles dissolve to feed them with solute. Both effects may operate in practice, since the high surface energy of very small precipitates gives them

Paper MG/C/72/52 of the Metal Physics Committee of the Metallurgy (General) Division of the British Iron and Steel Research Association, received 22nd May, 1952. The views expressed are the authors' and are not necessarily endorsed by the Committee as a body.

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a higher partial pressure than massive ones, and also makes them dissolve more easily. Dijkstra[17] and Wert[18] showed that precipitation from supersaturated solution in unworked iron becomes very slow once the amount left in solution is smaller than about 0·01%; the metal can remain supersaturated to this extent even after a month at room temperature.

It is possible to estimate roughly the effect of particle size on solubility. The equilibrium solubility $C\infty$ wt.-% in the presence of large particles at an absolute temperature T is taken to be given by

$$\log_e C\infty = 0{\cdot}936 - 9700/RT \quad \ldots\ldots(1)$$

for carbon below 723° C.,[16] and

$$\log_e C\infty = 0{\cdot}767 - 5300/RT \quad \ldots\ldots(2)$$

for nitrogen below 590° C.[19] These values apply after precipitation has occurred above 200° C., where the particles are large enough to be seen microscopically. When the particles are smaller than this, however, their partial pressure is greater, and more solute is held in solution. The concentration of solute, C wt.-%, in equilibrium with a spherical precipitate of radius r is given by[20]

$$\log_e (C/C\infty) = 2M\sigma/RT\rho r \quad \ldots\ldots(3)$$

where M is the molecular weight of the precipitated phase, σ is its surface energy, and ρ is its density. If the particle is a plate, the maximum degree of misfit probably occurs round the edge of radius r, in which case the solubility relation becomes[20]

$$\log_e (C/C\infty) = M\sigma/RT\rho r \quad \ldots\ldots(4)$$

There is doubt about the form in which iron carbide is precipitated. Internal friction measurements[18, 21] and some electron microscope photographs[23] suggest that the precipitates are spherical, whilst other electron microscope photographs suggest that they are plate-like.[24] On the basis of their own work, Wert and Zener[18, 21] took them to be spherical, and then estimated the number of precipitated particles per unit volume in their specimens. Most of their measurements refer to an ageing temperature above 120° C.[22] and a few to 37° C.[18] These results can be extrapolated to give estimates for the particle sizes after precipitation at temperatures of interest in the present work. The work of Van Vlack[25] and Smith[26] enables the surface energy of the ferrite–cementite interface to be estimated as about 700 ergs/sq. cm. Table I, which gives expected concentrations of carbon in solution after quench ageing at various temperatures, has been calculated from these estimates.

Table I

ESTIMATED CONCENTRATION OF CARBON IN SOLUTION AFTER QUENCH AGEING AT VARIOUS TEMPERATURES

Ageing Temp., °C.	Particle Radius, cm. (After Wert and Zener)	$C\infty$, wt.-% (Equation 1)	C, wt.-% (Equation 3)
45	$2{\cdot}54 \times 10^{-6}$	$6{\cdot}01 \times 10^{-7}$	$4{\cdot}5 \times 10^{-6}$
63	$2{\cdot}63 \times 10^{-6}$	$1{\cdot}36 \times 10^{-6}$	$8{\cdot}5 \times 10^{-6}$
120	$3{\cdot}02 \times 10^{-6}$	$1{\cdot}10 \times 10^{-5}$	$4{\cdot}3 \times 10^{-5}$
200	$4{\cdot}09 \times 10^{-6}$	$8{\cdot}91 \times 10^{-5}$	$2{\cdot}0 \times 10^{-4}$
300	$7{\cdot}44 \times 10^{-6}$	$5{\cdot}38 \times 10^{-4}$	$7{\cdot}9 \times 10^{-4}$

The corresponding information for nitrogen is not available. There is evidence[18, 23] that the nitride precipitated at temperatures below 200° C. is plate-like, and is probably the α'' phase.[27]

This discussion suggests that the rate of strain ageing, since it is controlled by the amount of carbon or nitrogen in solution, and by the size of the precipitates, should be affected by the amount of quench ageing the material is allowed to undergo before it is strained. The following experiments were made to test this conclusion. Apart from a preliminary hardness survey, the strain-ageing process has been followed purely from stress/strain curves taken on tensile wire specimens.

EXPERIMENTAL METHOD

The experiments were made on 2-mm. dia. wires of Remko iron, reference A/B/Ma, supplied by the British Iron and Steel Research Association, with the following composition in wt.-%:

Carbon	0·02	Tungsten	<0·02
Nitrogen	0·003	Vanadium	<0·01
Oxygen	0·028	Cobalt	<0·01
Silicon	<0·01	Copper	0·02
Manganese	0·15	Chromium	<0·01
Aluminium	0·01	Tin	<0·02
Titanium	<0·02	Lead	<0·05
Phosphorus	0·024	Molybdenum	<0·01
Sulphur	0·016	Zirconium	No trace
Nickel	0·03	Boron	No trace

A simple vertical tensile machine was made for straining the wires. The applied load was measured with an optical lever from the elastic deflection of a steel beam connected to the top of the specimen. A stiff beam was used to make the machine responsive to the yield phenomenon. The specimen was strained from a piston attached to its lower end and driven by hand through a gear system.

The specimen was gripped in long sockets fitted on its ends and fastened with grub screws. These sockets were attached to ball-and-socket joints in the elastic beam and the straining piston with long flexible links. This gave fairly axial loading; no attempt was made to improve the grips further, since, to start the specimen strain ageing, it was always necessary first to stretch it in the machine through the yield elongation, which aligned the load.

The extension of the specimen could be measured precisely, at small strains, by an extensometer on it, or less accurately, at large strains, by the movement of the straining device. To study the beginning of yielding accurately, it was necessary to observe strains down to 1% of the elastic strain at the yield stress. A form of Marten's extensometer was used in conjunction with a light beam reflected from the extensometer mirrors, and focused to an image in a travelling eyepiece. Working over a gauge length of 5 in., the strain sensitivity of this arrangement was better than 10^{-6}, and more than adequate for the task.

Premature yielding at stress concentrations near the grips was prevented by hardening the ends of the specimens in a liquid cyanide bath. Although subsequent heat-treatments caused spheroidization in these ends, the hardness remained three or four times greater than that of the material in the gauge length, and yielding never began there. The extensometer

knife edges were fixed to the hardened ends at points a few millimetres from the boundaries of the soft working part.

Strain-ageing treatments were given in oil baths at 30°, 38°, 45°, and 63° C. Quench-ageing treatments were given in similar baths at temperatures up to 300° C. Other heat-treatments at higher temperatures were made in an argon atmosphere; they did not sensibly affect the carbon and nitrogen content in the working part of the gauge length, or cause carbon to diffuse down from the carburized ends.

The sequence of operations in preparing a specimen was as follows:

(i) Specimen cut from wire and straightened
(ii) Gauge length plated with copper
(iii) Ends carburized at about 950° C.
(iv) Copper plating removed by etch in hydrogen peroxide and ammonia
(v) Heated at 930° C. for 30 min.
(vi) Cooled in furnace to 710° C. and held at this temperature for 30 min.
(vii) Quenched into water
(viii) Mounted in testing grips
(ix) Quench-ageing treatment, if any, given
(x) Strained 1% in tension
(xi) Strain-ageing treatment given
(xii) Stress–strain curve determined.

PRELIMINARY HARDNESS MEASUREMENTS

A preliminary survey was made to determine suitable quench-ageing treatments, and to compare the rates of strain ageing in specimens either freshly quenched or quench aged. The quench-ageing curves followed the usual shape,[6] and the times to reach the maximum hardness at various temperatures were as follows:

Temperature, ° C.	200	76	45	36	30
Time, min.	8	450	10^4	3×10^4	4×10^4

An activation energy of 19,500 cal./mole can be derived from these results that agrees with recent values for the diffusion of carbon[28] and nitrogen,[29] 20,100 and 18,200 cal./mole respectively.

The strain-ageing curves taken after deformations of up to 41% were again of the usual shape,[6] and showed the characteristics emphasized by Nabarro,[8] *i.e.*, the same final hardness after ageing at various temperatures, the absence of resoftening on prolonged ageing, and the much faster rate of strain ageing as compared with quench ageing. Activation energies for strain ageing were measured by comparing results at different temperatures. At the beginning of ageing,

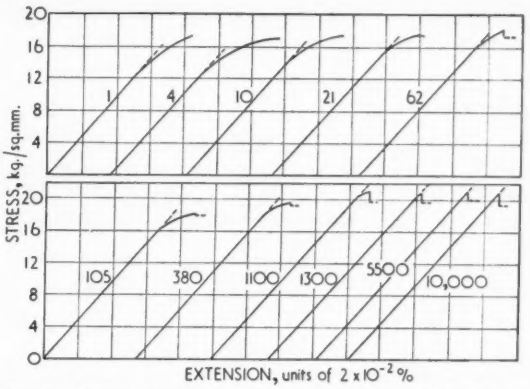

Fig. 2—Stress–strain curves for material W.Q. from 710° C., extended 1%, and strain aged at 30° C. Numbers show time of ageing in minutes

the energy was about 20,000 cal./mole, and at the end about 18,500 cal./mole. This difference is hardly outside experimental error, but may be significant because it appeared in several experiments.

Figure 1 shows two strain-ageing curves at 45° C.; curve *A* for a specimen strained and aged immediately after quenching, and curve *B* for one quench aged for 1000 min. at 200° C. before being strained. The time scale for curve *A* has been increased by a factor of 6·7 to bring the two curves roughly together. The effect of the quench-ageing treatment is to increase the total time for strain ageing by a factor of about 7.

STRAIN AGEING AND YIELD POINT MEASUREMENTS

A series of stress/strain curves taken on specimens that were quenched, immediately strained 1%, and then examined after strain ageing for various times at 30° C., is shown in Fig. 2. The following points are noteworthy:

(i) Rapid strain ageing can be detected from the rise in the limit of proportionality in the first few minutes after straining before the yield point has returned
(ii) In partly aged specimens, some plastic strain occurs before the fall in load at the yield point
(iii) In fully aged specimens, the upper yield stress is constant, and is not preceded by detectable plastic deformation.

From stress/strain curves such as these, strain-ageing curves were constructed that give the change in the limit of proportionality and the upper and lower yield points with time of ageing after straining

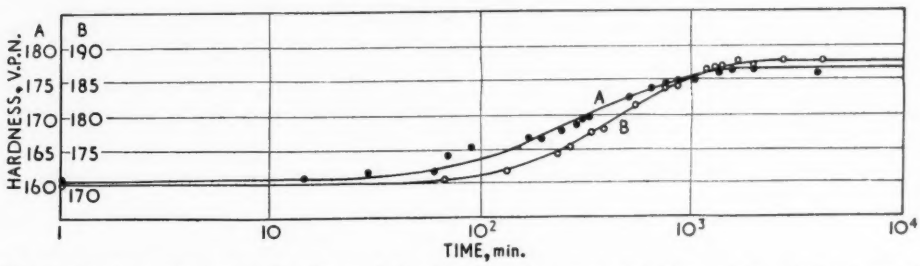

Fig. 1—Strain ageing at 45° C.: Curve *A*, freshly quenched material (time scale × 6·7); curve *B*, material quench aged for 1000 min. at 200° C.

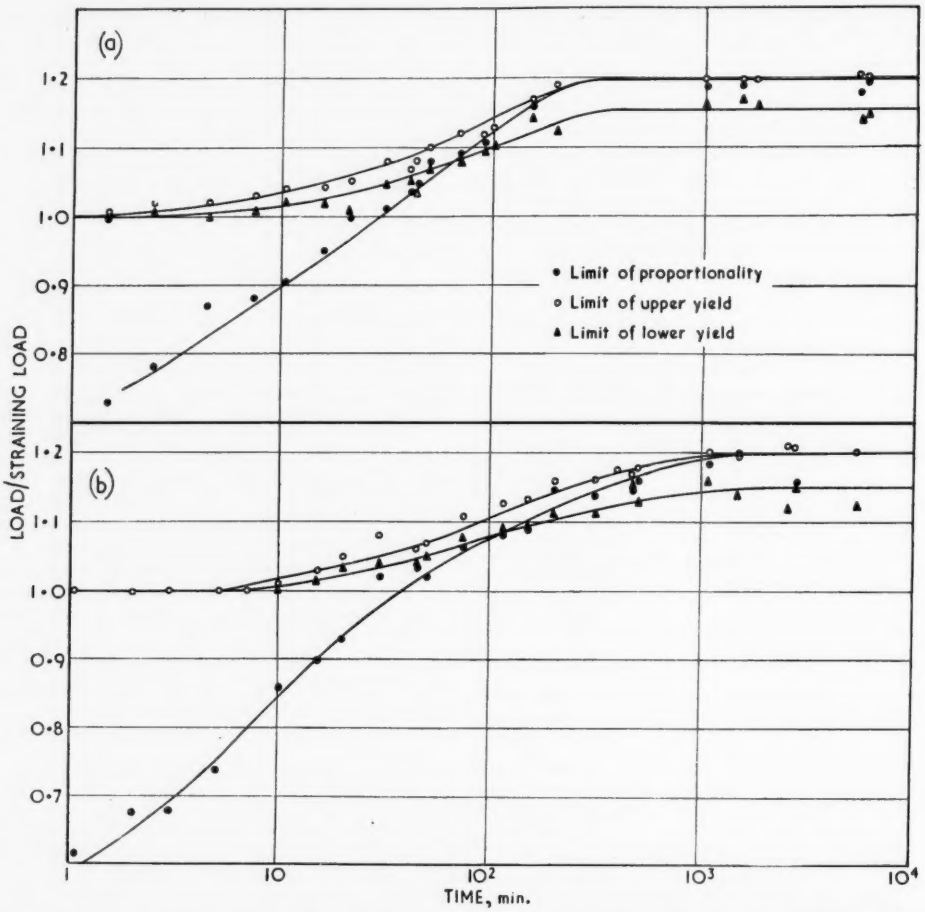

Fig. 3—Material (*a*) freshly quenched, and (*b*) quench aged at 200° C., both strain aged at 45° C.

Examples of these are shown in Figs. 3*a* and *b*. To reduce the effect of scatter from one specimen to another, the ratio of the actual load to the load used for the preliminary straining treatment was used in these diagrams. At small ageing times, where no yield point is discernible, this ratio was taken as unity, since large-scale plastic flow sets in at about the same load as is used for the preliminary straining treatment. Results similar to those shown in Figs. 3*a* and *b*, but differing in their time scales, were obtained at various strain-ageing temperatures and after various preliminary quench-ageing treatments. To study these, it is convenient to make one comparison in detail, *i.e.*, that between freshly quenched specimens and those quench aged at 200° C. for 1000 min., to demonstrate the principles involved. The other comparisons are reported more briefly.

Comparison of Specimens Freshly Quenched or Quench Aged at 200° C.

According to the dislocation theory,[9] the number of solute atoms transferred to unit length of a dislocation in the early stages of strain ageing is

$$n(t) = \alpha n_0 (ADt/kT)^{\frac{2}{3}} \quad \ldots\ldots\ldots\ldots\ldots\ldots (5)$$

where α is a number practically equal to 3, n_0 is the number of solute atoms in solution in unit volume, A is a parameter measuring the interaction between the dislocation and a solute atom ($A \sim 1{\cdot}5 \times 10^{-20}$ dyne/sq. cm. for carbon and nitrogen in α-iron), D is the diffusion coefficient of the solute, t is the time of strain ageing, k is Boltzmann's constant, and T is the absolute temperature. Harper[15] has recently obtained experimental confirmation of this equation. It predicts the initial rate of strain ageing to be proportional to the concentration of dissolved solute atoms.

From the carbon and nitrogen contents given in the introduction, the freshly quenched material should contain 0·02 wt.-% of carbon and 0·003 wt.-% of nitrogen in solution. At the start of strain ageing, both elements should collect in the dislocations at rates predicted by inserting the appropriate values for n_0 and D in equation (5). A concentration n_0 of nitrogen atoms should be equivalent in its ageing rate to a concentration $n_0(D_N/D_C)^{\frac{2}{3}} \simeq 2n_0$ of carbon atoms. Thus, the freshly quenched material should begin to age at a rate equivalent to a concentration of $0{\cdot}02 + (2 \times 0{\cdot}003) = 0{\cdot}026$ wt.-% carbon.

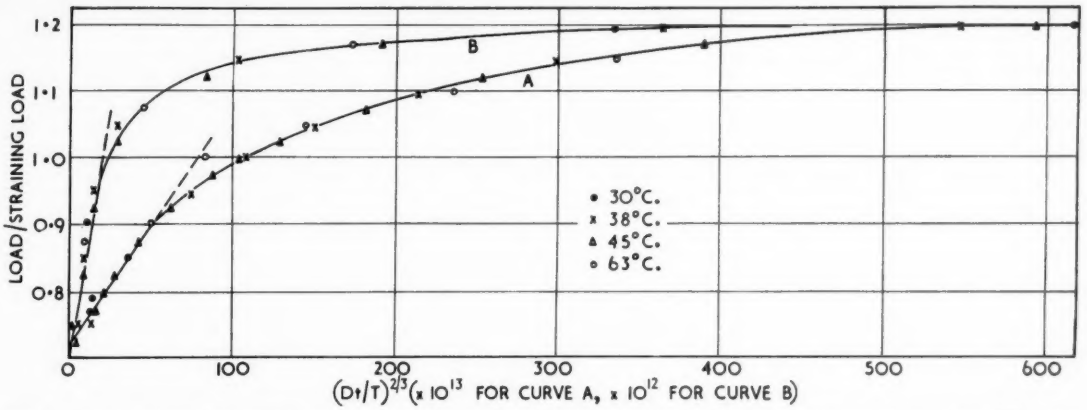

Fig. 4—Strain ageing at 45° C.: Curve *A*, freshly quenched material; curve *B*, material quench aged for 1000 min. at 200° C.

Material quench aged at 200° C. should retain all its nitrogen, 0·003% in solution according to equation (2). From Table I, the amount of carbon in solution should be either 0·00009% or 0·0002%, depending on the particle size. Taking account also of the smaller diffusion coefficient of carbon, these small amounts can be neglected in comparison with the nitrogen content. The rate of strain ageing from 0·003% of nitrogen is equivalent to that from 0·0054% of carbon. Thus, the freshly quenched material should begin strain ageing at a rate $0{\cdot}026/0{\cdot}0054 = 4{\cdot}7$ times greater than that quench aged at 200° C.

Figure 4 shows the increase in the limit of proportionality during strain ageing for material in these two conditions as a function of the variable $(Dt/T)^{\frac{2}{3}}$, suggested by equation (5). The diffusion coefficient of carbon has been used for the freshly quenched material, and that of nitrogen for the material quench aged at 200° C. The appropriateness of the use of these coefficients is suggested both by the considerations given above, and by the way they bring together results obtained at different strain-ageing temperatures. The initial parts of the two curves are shown as straight lines. The ratio of the slopes of these lines, when multiplied by the ratio of the diffusion coefficients, shows that the initial rate of strain ageing in the freshly quenched material is five times greater than that in the material quench aged at 200° C.

A further check on equation (5) can be made by using it, in conjunction with the experimental data in Fig. 4, to estimate the effective concentration of solute in the material. The freshly quenched material, for which the effective carbon content is 0·026 wt.-%, will be considered. Strain ageing is practically completed by the time that the ratio of the yield load to the straining load has reached 1·2. By extending the straight line in Fig. 4, for the freshly quenched material, to this level, the value of $(Dt/T)^{\frac{2}{3}}$ for complete strain ageing according to this $t^{\frac{2}{3}}$ law is $1{\cdot}4 \times 10^{-11}$. Strain ageing is assumed to be complete when each dislocation has collected one carbon atom per atom spacing along its length, *i.e.*, when $n(t)$ in equation (5) is about $3{\cdot}5 \times 10^{7}$ per cm. Substituting these values for $n(t)$ and $(Dt/T)^{\frac{2}{3}}$ in equation (5), and taking $\alpha = 3{\cdot}5$ and $(A/k)^{\frac{2}{3}} = 2{\cdot}8 \times 10^{-3}$, $n_0 = 2{\cdot}6 \times 10^{20}$ per c.c. The amount of carbon needed to give such a concentration in solution is 0·065 wt.-%. This is larger than the experimental value, 0·026 wt.-%, but correct in the order of magnitude, which is perhaps all that could be reasonably expected.

Specimens Quench Aged at 300° C.

These were quench aged for 90 min. before starting the straining and strain-ageing treatments. Strain ageing was studied at 45° C. only. The initial rate of strain ageing was 1·09 times as great as in the specimens quench aged at 200° C.

Material quench aged at 300° C. should contain 0·003% of nitrogen and 0·0008% of carbon in solution, which are equivalent to a concentration of 0·0033% of nitrogen. It should therefore strain age at a rate $3{\cdot}3/3 = 1{\cdot}1$ times faster than that quench aged at 200° C.

Specimens Quench Aged at 120° C.

These were quench aged for 8500 min., and the strain ageing was done at 45° C. The initial rate of strain ageing was 0·91 times as fast as that of the 200° C. series.

From Table I, the expected radius of the precipitated carbide particles is $3{\cdot}02 \times 10^{-6}$ cm., and the corresponding amount of carbon in solution should be $4{\cdot}34 \times 10^{-5}$ %. This amount can be neglected, particularly as carbon diffuses more slowly than nitrogen, in comparison with the expected amount, $2{\cdot}8 \times 10^{-3}$%, of nitrogen in solution (equation (2)). This material should therefore strain age at a rate $2{\cdot}8/3 = 0{\cdot}93$ times as great as that quench aged at 200° C.

Specimens Quench Aged at Lower Temperatures

These were quench aged at 63° C. for 21,000 min., or at 45° C. for 50,000 min. In neither case did the treatment cause the material to over-age substantially, and the precipitate remained too fine to be seen.

The analysis of the results is less straightforward than in the other cases, because the effect of small particle size can no longer be neglected. Taking

specimens quench aged at 200° C. as a standard for comparison, the initial rates of strain ageing for these materials, and the corresponding nitrogen contents, are as follows:

Quench-Ageing Temperature, ° C.	Initial Rate of Strain Ageing	Nitrogen Content Needed to Give this Rate, wt.-%	Equilibrium Nitrogen Content from Equation (2), wt.-%
200	1 (standard)	0·003 (standard)	0·003
63	0·77	0·0023	0·00084
45	0·63	0·0019	0·00056

The equilibrium nitrogen contents at 63° and 45° C. are clearly too small to explain the observed rates. The carbon contents are still smaller, even when the enhanced solubilities in the last column of Table I are used. This means either that the size of carbide particles is smaller than Wert and Zener estimated, or that the nitride particles are small enough to give an enhanced solubility of nitrogen. This second possibility seems reasonable. If a surface energy of 1000 ergs/sq. cm. is assumed for the nitride particles, then, from equation (4), a particle size of 2×10^{-6} cm. would be enough to give the necessary enhanced solubility. This size is comparable with that estimated by Wert and Zener for carbide particles.

Another indication of the importance of the fine particle size in these specimens is that, in the final stages of strain ageing, the rate was 2–3 times faster than that of specimens quench aged at 200° C. This suggests that particles are redissolving towards the end of strain ageing to feed the dislocations with solute. This process is obviously faster when the particles are more finely dispersed, since not only do small ones dissolve more easily, but also their average distance from dislocations is smaller.

CONCLUSIONS

These experiments show that the observed rate of strain ageing depends on the initial heat-treatment of the material. Freshly quenched specimens that have a maximum amount of carbon and nitrogen in solution strain age about five times faster than those that have been quench aged at temperatures (120–300° C.) where most of the carbon and nitrogen is precipitated out of solution. The rate of strain ageing is closely related to the amounts of carbon and nitrogen in solution and to their rates of diffusion.

The experimental results show a simple proportionality between the initial rate of strain ageing and the concentration of dissolved solute; this accords with the theoretical prediction. The absolute rate of strain ageing can also be estimated from the theory, and is of the correct order of magnitude. After quench ageing at lower temperatures (63° and 45° C.), the rate of strain ageing is much higher than would be expected from the equilibrium solubility of carbon and nitrogen. This effect can be explained semi-quantitatively in terms of the fine scale of the precipitation at these temperatures, since solubility is increased when the particle size is small.

A suitable quench-ageing treatment, *e.g.*, at 200° C., can reduce the amount of carbon in solution to such a low value that strain ageing due to carbon becomes extremely slow. On the other hand, in view of the relatively high solubility and diffusion rate of nitrogen, quench ageing should not be very effective in reducing strain ageing due to nitrogen. For making 'non-ageing' steel, the best procedure would seem to be to remove the nitrogen from solution by the addition of an element such as aluminium, with a strong affinity for it, and to remove the carbon from solution by a preliminary heat-treatment at, say, 200° C.

Acknowledgment

The authors wish to thank Professor D. Hanson, Director of the Metallurgy Department, University of Birmingham, for provision of research facilities.

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The Formation of Bainite

By T. Ko and S. A. Cottrell

SYNOPSIS

When bainite is formed from austenite, the specimen surface is distorted, giving a structure similar to that obtained during the formation of martensite. By studying the surface relief during the formation of bainite, using a specially developed metallographic technique and hot-stage microscopy, it has been established that bainite forms by nucleation and slow *coherent* growth, the rate of which depends on the composition of the austenite and the temperature of transformation. A new theory of the mechanism of bainite formation is proposed. It is suggested that owing to the insufficiency of the driving force, martensite cannot form, and the coherent growth is possible only if the driving force is increased and the strain due to the density change is reduced by lowering the carbon in solution in the bainite. This takes place by diffusion of carbon into the surrounding austenite or by precipitation of carbide within the bainite, or by a combination of both ; the rate of growth is therefore controlled by the rate of carbon removal.

Introduction

When austenite is isothermally transformed at temperatures in the intermediate range, an acicular aggregate of ferrite and carbide particles forms, and is generally known as bainite. The structure and the mechanism of formation of bainite have been extensively studied since the work of Davenport and Bain.[1] The mechanism, however, remains obscure, and the present investigation attempts to elucidate this fundamental problem.

The structure of bainite varies with the composition of the austenite from which it is formed and with the temperature of its formation. Both the degree of darkening during etching and the acicularity become more pronounced as the temperature of formation is lowered. In plain carbon steels, bainite formed just below the ' nose ' of the isothermal transformation diagram—often called ' upper bainite '—is feathery and open in structure, consisting of adjacent parallel ferrite plates containing coarse carbide particles. Bainite structures formed at temperatures just above the M_s point, however, resemble tempered martensite, and their substructures are irresolvable under optical microscopes. These structures—generally referred to as ' lower bainite '—differ from martensite in that, in the same steel, bainite is often thinner than the martensite formed in the same temperature range.[2, 3] Electron microscopical investigations[4] showed that lower bainite contains carbide particles preferentially orientated, probably along {111} planes of the austenite. In alloy steels containing carbide-forming elements, only iron carbides (with little partition of the alloying elements) have been found in bainite, but a redistribution of alloying elements may take place during prolonged annealing.[5–10]

The carbide in bainite formed above 300° C. is cementite,[7, 11, 12] but the nature of the carbide in bainite formed below 300° C. is not certain.[11–13] The orientation and habit planes of the carbides in bainite are not known. The orientation of cementite in martensite tempered at 600° C. in a 1% C steel has been reported as $(103)_{Fe_3C}//(110)_\alpha$ and $[010]_{Fe_3C}// [111]_\alpha$.[14]

The physical and chemical conditions of austenite can be affected by the formation of bainite. Numerous workers have found that partial transformation in the bainite range lowers the M_s of the remaining austenite.[15–23] Stabilization of austenite can also take place when the austenite is held in the bainite range for times less than the incubation period.[24, 25]

Direct measurements of austenite lattice parameters, or measurements of c/a ratios of the martensite formed from the residual austenite, have shown that there is an increase in the carbon content in the austenite during the formation of bainite in hypo-eutectoid vanadium[26] and chromium steels.[27] There are indications that carbon enrichment in austenite also occurs in eutectoid steels.[28] Such an effect has not been found in hyper-eutectoid chromium steels,[20] or in 5% Ni steel[29] during the formation of lower bainite.

Previous Suggestions Regarding the Mechanism of Bainite Formation

The acicular dark-etching constituent that forms at intermediate temperatures was originally named ' martensite–troostite ' by Davenport and Bain,[1] as they suspected that it formed as martensite but tempered rapidly. This idea was elaborated by Vilella, Guellich, and Bain,[30] who said that the acicular transformation products form by " the successive, abrupt formation of flat plates of supersaturated ferrite along certain crystallographic planes of the austenite grains; this supersaturated ferrite begins at once to reject carbide *particles* (not lamellæ), at a rate depending upon temperature." Wever,[12] Portevin,[31, 32] and their co-workers had previously suggested that bainite forms as supersaturated ferrite.

Indications that the length and thickness of the bainite plates in a series of specimens increased with increasing holding time were found by Smith and Mehl,[33*] who suggested that bainite probably formed by a process of nucleation and growth, ferrite being the nucleating phase. Förster and Scheil[34] also suggested that bainite forms by a slow growth process but is crystallographically similar to martensite.

Paper MG/A/153/52 of the Alloy Steels Committee of the Metallurgy General Division of the British Iron and Steel Research Association, received 29th July, 1952. The views expressed are the authors' and are not necessarily endorsed by the Committee as a body.

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* They also observed that the orientation relationship between austenite and the ferrite in bainite varied only slightly with the temperature of formation and was similar to that between martensite and austenite. The habit planes of bainite, however, changed markedly with the temperature of transformation. In a eutectoid steel, the habit planes of bainite were very near to those of pro-eutectoid ferrite when it formed at 450° C., but approached those of martensite as the temperature of formation was lowered.

After an extensive study of the structures of bainite, Hultgren[6] proposed that the reaction proceeded by nucleation and growth of ferrite, enriching the austenite with carbon until cementite rods were precipitated; these were then surrounded by the growing ferrite. Greninger and Troiano[16] also suggested that bainite formed directly as an aggregate and not as single-phase crystals.

Klier and Lyman[27] proposed that in hypo-eutectoid steels, carbon movement in austenite led to the formation of regions with high- and low-carbon contents. The low-carbon austenite then transformed to martensite, which immediately decomposed into bainite. A similar view was held by Wiester.[35]

Theoretical attempts were recently made by Zener[36] and Fisher[37] to calculate the shape of the isothermal transformation curves for bainite on the assumption that bainite formed as supersaturated ferrite, free from strain, by a mechanism similar to that of martensite formation. They suggested that there is an upper temperature limit of bainite formation, B_s, which is the temperature at which the supersaturated ferrite is in thermodynamic equilibrium with austenite, with (Fisher) or without (Zener) carbon enrichment. Fisher[37] also proposed that bainite (or delayed martensite) was nucleated by those embryos—*i.e.*, carbon-free regions of ferrite in austenite, which had been frozen in from high temperatures—that had grown to sizes large enough for nucleation of martensite.

EXPERIMENTAL METHODS

In spite of the vast amount of work that has been done, fundamental questions related to the formation of bainite remain unanswered:

(i) Whether the austenite and bainite lattices are incoherent, as in the case of ferrite and austenite, or coherent, as in that of martensite and austenite
(ii) Whether bainite forms, like ferrite and carbide, by nucleation and growth, or, like martensite, in a fraction of a second
(iii) Whether there is really any fundamental difference in the mechanism of formation of ' upper ' and ' lower ' bainite.

The first question can be studied by examining the surface of specimens after bainite formation. In the austenite–martensite transformation, the homogeneous distortion and the coherency of the lattices give a relief effect on the surface. In the present investigation, therefore, the distortion of the specimen surface due to the formation of bainite was examined. Studies can be made on specimens which have been either partially or fully transformed to bainite. In the former case, the residual austenite, which would transform to martensite on cooling, can be removed by isothermal annealing in the pearlite range. Any relief on the surface is then due to bainite formation, because pearlite does not produce any relief effect. For this purpose, it is therefore necessary to select steels that can transform to pearlite, or to pro-eutectoid ferrite, or carbide, and pearlite, by incoherent processes within a reasonable period.

The details of the methods used are described on p. 309. It has been found that bainite forms by coherent growth from centres that can be called nuclei. As the growth is relatively slow, and the bainite can be seen in relief, the transformation can be followed by hot-stage microscopy. These investigations have also led to the conclusion that bainite forms by the same mechanism throughout the whole range.

Materials Used

The steels investigated, given in Table I, covered a wide range of composition. They were chosen for one or more of the following reasons:

(i) The steel undergoes complete transformation to bainite in a certain temperature range
(ii) The steel undergoes full transformation to pearlite after partial decomposition to bainite
(iii) The steel exhibits separate pearlitic and bainitic ranges
(iv) The steel has a long incubation period and a low M_s point.

Steels En 18, 21, 26, and 31 were taken from stock used in determining the isothermal transformation diagrams published in Special Report No. 40.[38]

Experimental Procedure

Isothermal Transformation Diagrams—For determining the isothermal transformation diagrams of the steels, microscopic and dilatometric methods were used. In the microscopic method, specimens (0·110 in. dia. and 0·2 in. long) of different steels given the same austenitizing and isothermal transformation treatments were heated in argon in the furnace

Table I
ANALYSES OF STEELS USED*

Steel	Analysis, %								Steelmaking Process
	C	Ni	Mn	Cr	Mo	Si	S	P	
En 17	0·37	...	*1·51*	...	*0·45*	0·17	0·035	0·027	...
„ 18	0·48	0·18	0·86	*0·98*	0·04	0·25	0·021	0·023	Acid O.H.
„ 19	0·41	...	0·64	*1·37*	*0·28*	0·25	0·030	0·027	...
„ 21	0·33	*3·47*	0·74	0·07	...	0·23	0·027	0·031	Acid O.H.
„ 25 *VA*	0·33	*2·59*	0·68	*0·53*	*0·58*	0·20	0·029	0·029	Acid O.H.
„ 25 *VB*	0·32	*2·60*	0·63	*0·51*	*0·55*	0·22	0·023	0·023	Basic elec. arc
„ 26	0·42	*2·53*	0·67	*0·72*	*0·48*	0·31	0·022	0·029	Acid O.H.
„ 31	1·08	0·33	0·53	*1·46*	0·06†	0·25	0·015	0·022	Elec. arc
„ 44 *T*	0·85	0·09	0·64	0·02	0·01	0·20	0·027	0·034	H.F. induction

* Figures for principal alloying elements are indicated in italics
† En 31 contained also 0·11% W

described previously.[39] Suitable precautions were taken to avoid contamination. The salt bath used for isothermal decomposition was placed below the austenitizing furnace so that very rapid (within 0·5 sec.) transference of specimens could be made. After a given time of isothermal treatment, the specimens were quenched in water. For steels *VA* and *VB*, specimens of 0·25 in. dia. and 0·25 in. length were also used. These were prepared from bars that gave the 0·110-in. wire rods after repeated cold-drawing and annealing. No difference in the transformation behaviour in the bainitic range was observed between the two sizes.

Dial gauge dilatometers were used. A specimen of 0·110 in. dia. and 1 in. length was placed in a silica tube, which was cut open near the bottom to accelerate quenching. The whole assembly was heated in a platinum-wound high-temperature furnace. Decarburization was prevented by nickel-plating the specimen and by placing the silica tube in a graphite sheath inside the furnace tube with a nitrogen atmosphere. No significant diffusion of nickel into the specimen was observed. The use of an argon atmosphere gave identical results. Lead baths were used for isothermal decomposition treatments.

The temperature of the austenitizing furnace was controlled to within $\pm 5°$ C., and that of the isothermal transformation baths to within $\pm 2°$ C. In all cases, the time required to heat the specimens to the austenitizing temperature was about 2 min., which was included in the total austenitizing time of 15 min. The time required to cool the specimen from 950° C. to a temperature 5° C. above the isothermal transformation temperature was 12–15 sec. in the dilatometric method, and in the microscopic method it was 18–20 and 25–28 sec. for specimens of 0·110 in. and 0·25 in. dia., respectively. The difference in cooling rate had no effect on the results reported here. The incubation period given on the isothermal transformation diagrams indicates the time required—including that required for cooling—to give 3% transformation, and it is based mainly on the results obtained microscopically.

Observation of Bainite Formation by Surface Relief Effects—To study the surface relief of specimens during bainite formation, specimens with metallographically polished surfaces, 0·110 × 0·5 in., were sealed *in vacuo* into a thin-walled silica tube. Each such specimen was then heated to the austenitizing temperature (1250° C. for steels En 31 and En 44; 1350° C. for the other steels) for 30 min., quenched into a lead or salt bath, and maintained at the isothermal transformation temperature for a given period. The austenite remaining after the desired transformation was then removed by transferring the sealed tube into a second furnace, where it was kept at a temperature at which it rapidly decomposed to pearlite. The time required for complete decomposition was either taken from the published diagrams, allowing for the change in austenitic grain size, or determined experimentally. After the treatment, the specimen was cooled to room temperature and its surface was examined.

Hot-Stage Microscopy—To obtain a continuous record of the progress of bainite formation, a hot-stage was used in conjunction with a vertical bench microscope. Steel En 31 was chosen for this investigation. The specimen was austenitized at 1250° C.—as already described—for 1 hr. in a sealed silica tube, and then quenched to a temperature 30° C. above M_s. The hot-stage was maintained at the same temperature. The silica tube was then broken, and the specimen was rapidly transferred to the hot-stage. After adjusting the position of the specimen, a silica window was placed above it and the chamber was evacuated. The specimen was then rapidly reheated to the required temperature, at which it was maintained. The complete operation of transferring the specimen and heating it to the transformation temperature took about 15 min., but the time during which the specimen was exposed to air was so short that no significant oxidation occurred. This method avoided the difficulties of constructing a hot microscope stage in which the specimen could be heated to high austenitizing temperature and rapidly quenched to the bainitic range. When the specimen was held at a lower temperature prior to isothermal transformation, the surface structure was unaltered, but the incubation period and the rate of transformation at the second temperature were affected, as will be seen in a later section. The times

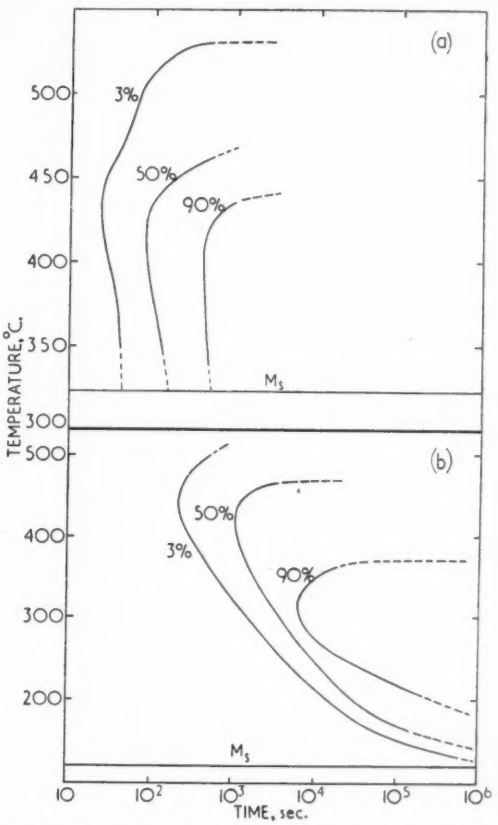
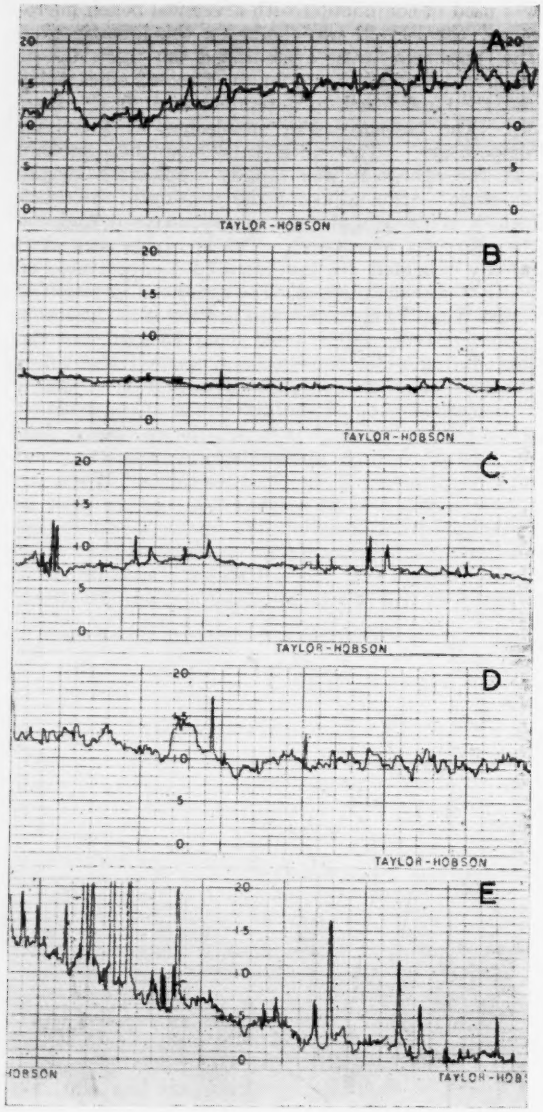


Fig. 1—Isothermal transformation diagrams in the bainite range: (*a*) Steel En 25 (austenitizing temp. 1350° C.); (*b*) steel En 31 (austenitizing temp. 1250° C.)



A. Martensite
B. Pearlite
C. Pearlite and bainite
D. Bainite
E. Bainite

Fig. 2—Stylus traces of surfaces of steels after austenite decomposition (horizontal × 100, vertical × 10,000. Reduced by one-half linear in reproduction). *A–D*, steel En 31 ; *E*, steel En 21

given for the photographs reproduced were taken from the instant when the specimen reached the transformation temperature.

In the present investigation, high austenitizing temperatures were used. This procedure had two advantages: it produced very coarse austenitic grains (ASTM No. 2 to 1) in which the development of bainite could be easily followed, and it reduced inhomogeneity in the austenite. The effects of austenitizing temperature on the intermediate transformation have been separately studied and will be reported in another paper.

EXPERIMENTAL RESULTS

Isothermal Transformation Diagrams

Isothermal transformation diagrams for the bainite range were determined for all the steels except En 26 and 44 *T*. They are similar to those given in the Atlas,[38] except for steels En 25 and 31, the diagrams of which are shown in Figs. 1*a* and *b*. Typical structures of the transformation products are shown in Figs. 3*a*, *b*, and *c*. Compared with the bainite formed in steels austenitized at 950° C., the transformation products in the present case were coarser and more acicular, particularly in the chromium-bearing steels, En 18, 19, 25, and 31. For example, in steel En 31, the product was formed in blocks at 465° C. after austenitizing at 950° C. for 60 min., but it was acicular after austenitizing at 1250° C. The tendency to form 'feathery' bainite in the upper temperature range was increased by the use of a high austenitizing temperature in the case of steels En 17, 19, and 21. The bainite formed below 500° C. was dark-etching (with nital) in steels that contained chromium or molybdenum as the principal alloying elements, *i.e.*, En 18, 19, and 31. In all steels, the fineness of the structure within the bainite increased with decreasing temperature.

The Formation of Bainite

Photographs of the surface structures found in some of the steels after complete decomposition to bainite are shown in Figs. 4*a* and *b*. All the steels investigated exhibited relief patterns, and in all cases individual needles were clearly visible. These needles are very similar to those of martensite, each projection having one side in shadow and the other illuminated, according to the direction of the light. Moreover, the height of the needles diminishes towards their ends. A relief pattern of this type can only be formed by a process in which the lattices of the parent and the new phase are coherent during growth. The needles do not cross the grain or the twin boundaries of the austenite. A few specimens, having surface relief structures, have been examined by means of a surface-finish measuring machine. Some of its stylus traces are shown in Fig. 2. There is a marked similarity between the traces of martensite *A* and bainite *D* and *E*, whereas those of pearlite *B* are very different.

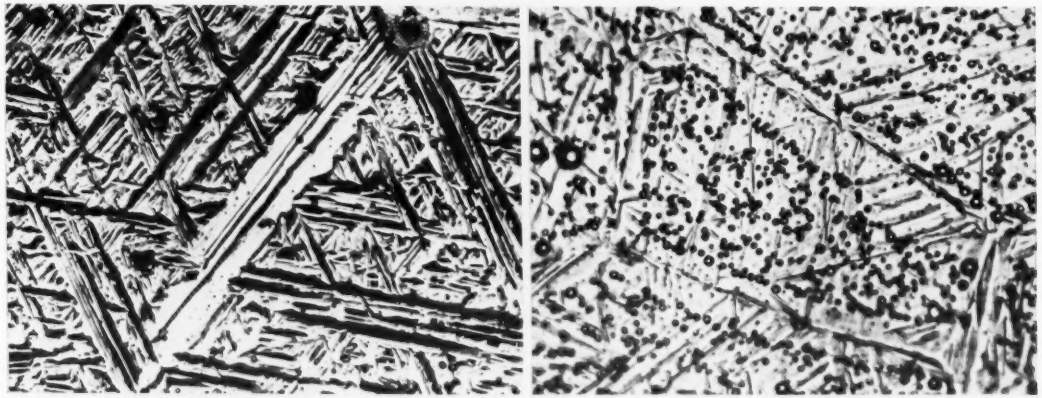
These relief structures were observed in all specimens when bainite was present, and the temperature ranges over which observations have been made are as follows:

Steel	M_s, ° C.	Temperature Range, ° C.
En 17	300	330–480
,, 18	290	330–530
,, 19	300	330–480
,, 21	310	330–530
,, 25 *VA*	320	330–505
,, 25 *VB*	320	330–435
,, 26	270	300–445
,, 31	118	200–480
,, 44 *T*	200	350–475

The progress of isothermal transformation at 275° C. in steel En 31 is shown in Figs. 5*a*, *b*, and *c*. The specimen shown in Fig. 5*a* was held at the temperature for only 6 min., *i.e.*, within the incubation period. Subsequent decomposition in the pearlitic range gave the specimen a flat surface, except at the austenitic

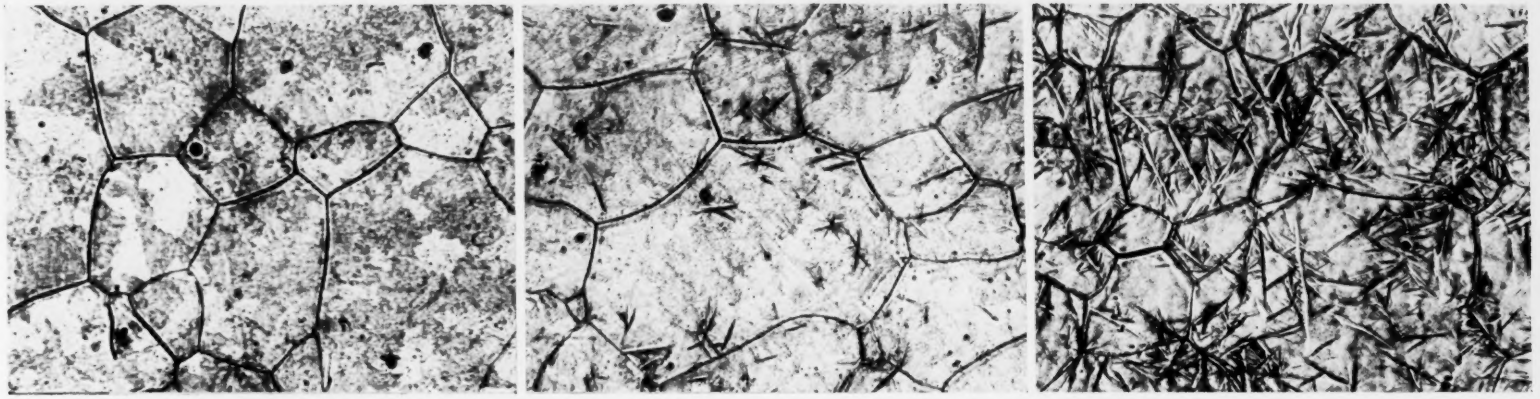
(a) (b) (c)

Fig. 3—Typical structures produced by isothermal transformation : (a) Steel En 17, austenitized at 1350° C. and transformed at 450° C. for 15 sec. × 500 ; (b) steel En 31, austenitized at 1250° C., transformed at 295° C. for 2 hr. (structure near the surface) × 250 ; (c) steel En 25, austenitized at (i) 950° C., (ii) 1250° C., and transformed at 465° C. for 200 sec. × 150 All specimens etched with nital



(a) (b)

Fig. 4—Surface structures : (a) Steel En 21 (bainite formed at 360° C.) ; (b) steel En 25 (bainite formed at 350° C.) × 500



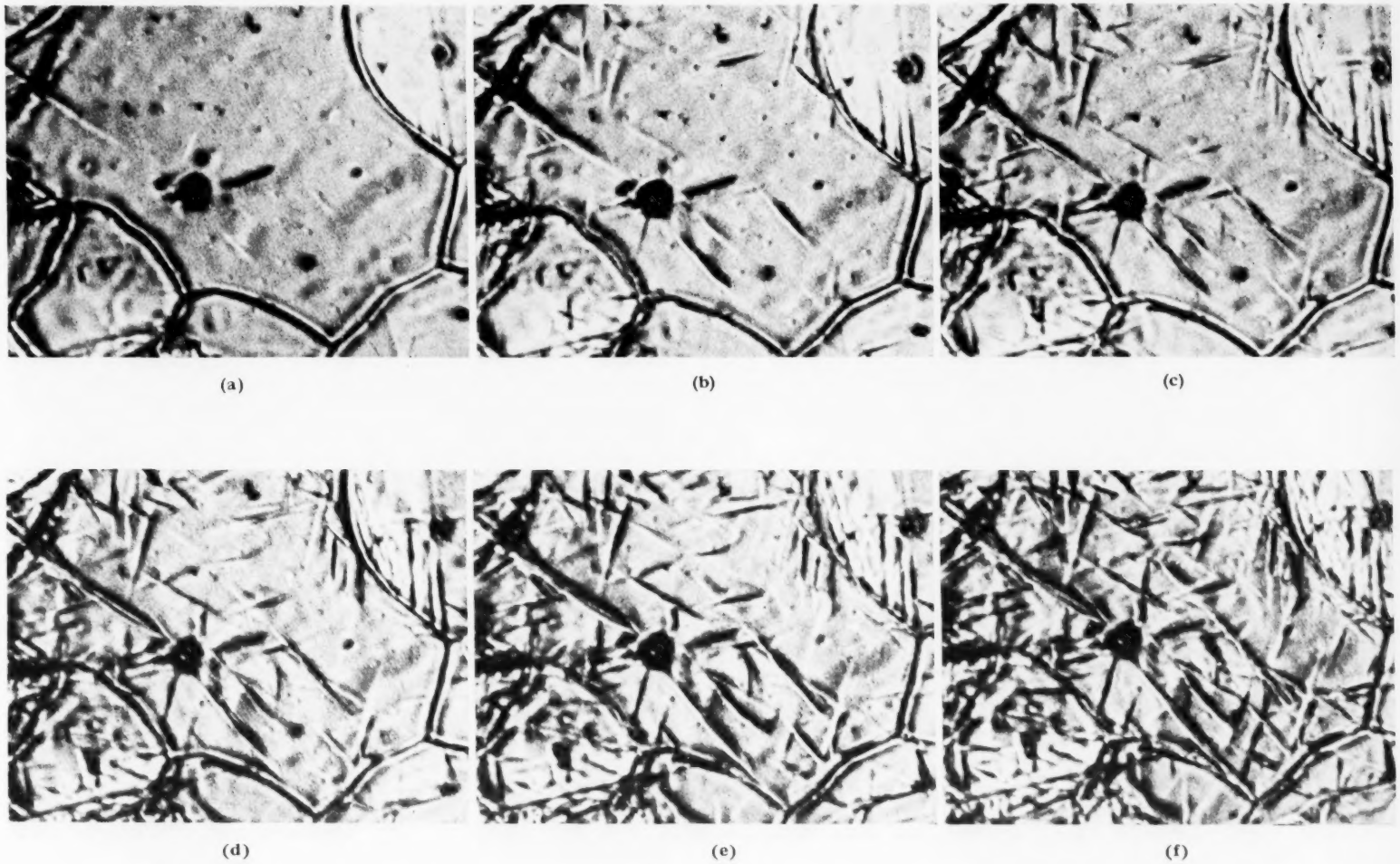
(a) (b) (c)

Fig. 5—Surface structures of steel En 31, austenitized at 1250° C., quenched to 275° C. for (a) 6 min., (b) 30 min., and (c) 45 min., and reheated to 600° C. for 2 hr. × 400

(a) (b) (c)

Fig. 6—Surface structures : (a) Steel En 21, austenitized at 1350° C., quenched to 370° C. for 100 sec., and reheated to 600° C. for 90 min. × 500 ; (b) steel En 21, austenitized at 1350° C., quenched to 600° C. for 1 hr., cooled to 360° C. and held for 30 min. × 500 ; (c) steel En 25, austenitized at 1350° C. and isothermally transformed at 400° C. Scratched before transformation × 800

austenitized at 1350° C. and isothermally transformed at 400° C. Scratched before transformation × 800



(a) (b) (c)

(d) (e) (f)

Fig. 7—Surface structures of steel En 31, austenitized at 1250° C., quenched to 150° C., reheated to and transformed at 275° C. : (a) After 15 min. ; (b) after 17 min. ; (c) after 18 min. ; (d) after 19·5 min. ; (e) after 22 min. ; (f) after 29 min. × 100, enlarged 9 times

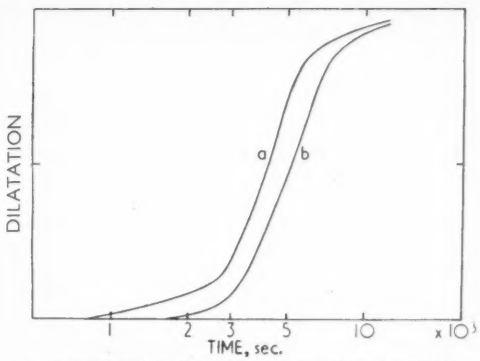
grain boundaries that had been developed at high temperature by thermal etching. The pearlitic colonies within the grains had been etched slightly by the traces of air left in the silica tube. Figure 5*b* shows the early stages of the transformation in another specimen that was held at 275° C. for 30 min. The bainite needles at this stage often originated from the same place and were all of similar length. At a later stage (Fig. 5*c*), new bainite needles appeared among the larger ones. The growth of the needles always stopped when they reached the austenite grain or twin boundaries or other needles. After polishing, these needles on the surface were shown to correspond to bainite needles; they were embedded in a matrix of fine pearlite.

The complete growth of bainite needles took as much as 30 min. in steel En 31 at 275° C., but was more rapid in other steels in which the transformation, as shown by the diagrams, is faster. For instance, in steel En 21 the bainite had grown across an austenite grain in less than 30 sec. at 500° C. Even at 350° C., specimens of steels En 21 and 25 which had been held for 20 sec. already contained bainite needles larger than those shown in Fig. 5*c*. The needles seen in relief were usually coarser in low-carbon than in high-carbon steels (*cf.* Figs. 4*a* and 5*c*).

In steels En 25 *VA* and *VB*, marks indicating evolution of gas during the heat-treatment were sometimes observed (Fig. 4*b*). The regions near the grain boundaries were usually free from these marks. No definite explanation can be offered at present, but it seems very probable that the gas in the regions near the boundaries had escaped along the interfaces, whereas the gases within the grain could escape only through the surface.

To confirm that the relief pattern on the surface was caused by the formation of bainite and not by a precipitation of ferrite, specimens of steel En 21 were austenitized at 1350° C. and then (i) partially decomposed in the bainitic range (370° C.), followed by isothermal annealing in the pearlitic range (600° C.); and (ii) decomposed first at 600° C., to precipitate ferrite at the grain boundaries, followed by isothermal treatment at 370° C. Figure 6*a* shows the surface of the specimen after the first heat-treatment. In some cases, the bainite plates had extended across the grain from boundary to boundary. In the second specimen (Fig. 6*b*), however, the grain boundaries became enveloped by the ferrite precipitated during the first isothermal decomposition in the upper range. The bainite that was subsequently formed at a lower temperature was limited to that part of the austenite where no ferrite had precipitated. It is clear that the precipitation of ferrite or pearlite, which takes place by an incoherent process, does not give the relief effects observed after martensite and bainite formations, which are coherent processes. The bending of scratches on the specimen surface also shows that bainite is coherent with the austenitic matrix during growth. Figure 6*c* illustrates the distortion of a straight marking by the formation of bainite in steel En 25. Formation of martensite has the same effect on surface scratches.

It is clear from the above results that bainite forms



a. Quenched first to 150° C. for 5 min. and reheated to 300° C.
b. Quenched directly to 300° C.

Fig. 8—Dilatation/time curves: Steel En 31, austenitized at 1250° C.

by coherent growth from nuclei. The rate of growth varies with the temperature, but it is very slow compared with the formation of martensite, which takes place in less than 10^{-4} sec.[34] Hot-stage microscopic observations on steel En 31 at 275°, 300°, and 350° C. confirmed that bainite forms by continuous growth and not by instantaneous increments. Figure 7 shows the growth of bainite during transformation at 275° C. The width and the length of the needles increased with time, and the height of the projections also increased as the needles were growing. The rate of growth along the habit plane was much greater than in other directions.

Effect of Martensite

In one specimen of steel En 31, part of the specimen was cooled to just below M_s before transference to the hot-stage. A few martensite needles were observed on the surface extending across the grains, and the rest of the specimen remained flat. During isothermal transformation on the hot stage, bainite was observed to form first adjacent to the martensite, usually at an angle. The incubation period was reduced to one-third in the presence of martensite.

Effect of Step-Quenching

A few experiments were made to investigate the effect of holding the specimens at 150° C. before beginning the isothermal transformation at higher temperatures. Dilatometric studies were made on steel En 31 by (i) quenching the specimen directly from the austenitizing temperature to 300° C., and (ii) quenching first to 150° C. and holding at this temperature for 5 min. before the specimen was heated to 300° C. Figure 8 shows the dilatation/time curves at 300° C. The low-temperature treatment reduced the incubation period at the isothermal transformation temperature and increased the rate of transformation.

DISCUSSION

The present investigation has shown that bainite forms by nucleation and *coherent* growth at a rate that depends on the temperature and the composition of the austenite. Any theory of bainite formation must account for this observation, in addition to the previous ones briefly summarized in the introduction. Moreover, an explanation of the difference between

martensite and bainite is required, especially in regard to the speeds of formation of their individual needles. The following proposals for the mechanism of bainite formation are consistent with these facts.

The nucleation of a new phase, which is coherent with the austenite, can lead to the formation of either martensite or bainite. The actual temperature at which martensite begins to form, M_s, lies below the temperature M_0, at which the free energies of austenite and supersaturated ferrite are equal.[36, 40] Thus, sufficient driving force must be provided for the increase in surface energy and volumetric and shear-strain energies, and probably also to overcome the resistance in the matrix to the movement of the coherent interface.[24] Unless the temperature is sufficiently low, the free-energy difference between the austenite and the supersaturated ferrite is not large enough for the formation of martensite.

Growth can take place, however, if by some process or processes, the balance of energies becomes more favourable. In the temperature range in which carbon diffusion can occur, the strain energy due to the density change can be reduced by lowering the amount of dissolved carbon in the supersaturated ferrite. This process will also increase the driving force for the transformation. When this takes place, the nucleus of bainite can grow, and will continue to grow, provided that thermodynamic requirements are fulfilled by a continuous removal of carbon, and that the coherency of the austenite and bainite lattices is maintained.

The removal of carbon can take place either by precipitation in the form of carbide, or by diffusion from the bainite into the surrounding austenite, or by a combination of both. The relative contribution by these two mechanisms depends on the rates of diffusion of carbon in the two structures, the activity of carbon in austenite, and the rate of precipitation of carbides in bainite. It is expected that at high temperatures where the diffusion of carbon in austenite is relatively rapid, the removal of carbon from the bainite will occur mainly by diffusion from bainite into austenite, especially in low-carbon steels in which the carbon potential in the austenite is low. At low temperatures, precipitation within bainite will be dominant. According to the above mechanism, the rate of coherent growth of bainite plates will depend on the rate of removal of dissolved carbon from bainite, and thus it cannot equal that of martensite.

Clustering of carbon, in the form of regions of austenite containing a high concentration of interstitial carbon atoms, may take place during incubation. The number of clusters of a given size will be proportional to $e^{-\Delta G/RT}$, where ΔG is the free-energy increase due to clustering; it will increase with decreasing temperature. These clusters will act as embryos for carbide precipitation.

When carbide-forming elements, such as Cr, Mo, and V, are present, there will be a tendency for carbon atoms to cluster around their atoms; more carbon atoms will be firmly anchored when the concentration of these alloying elements is increased. The reduction in mobility of carbon atoms in austenite and in bainite, due to their association with these alloying elements, will hinder the removal of carbon, and the growth of bainite plates will be slow. If the alloy contains a small amount of carbon relative to the concentration of strong carbide-forming elements, the formation of bainite will be extremely difficult, because all the carbon atoms are anchored. It is known that austenite containing 9% of Cr and 1% of C will not transform to bainite in 200 days.[20]

A bainite plate will stop growing (i) when it encounters an obstacle such as a grain boundary or another plate; (ii) when the coherency of the two lattices is destroyed by an interfacial precipitation of carbide, and (iii) when the enrichment of carbon in the austenite sufficiently reduces the driving force of the transformation. At high temperatures near B_s, the rate of nucleation is low, because the free-energy difference between austenite and bainite is small; the tendency for precipitation at an interface is great, and the growth of bainite will soon cease when the interfacial precipitation destroys the coherency. These, together with the carbon enrichment of austenite, which has a significant effect when the driving force is small, probably explain the fact that at high temperatures the austenite does not transform completely to bainite. Transformation is either extremely slow or is succeeded by the formation of pearlite.

CONCLUSIONS

The distortion and the coherency of the lattices during the austenite–bainite transformation produce on the surface of a specimen a relief effect similar to that due to the formation of martensite. This enables a study of the mode of formation of bainite to be made. It has been found that bainite forms by nucleation and coherent growth, the rate of which depends on the composition of austenite and increases with increasing temperature. The rate of growth is, however, very slow compared with that of martensite.

It is suggested that the free-energy difference between austenite and the supersaturated ferrite is not sufficiently large to allow the formation of martensite, owing to the adverse strain. Nevertheless, coherent growth can take place when the strain due to the density change is relieved, and the driving force for the transformation is increased if the amount of carbon dissolved in the bainite is reduced. This can be achieved either by carbon diffusion from bainite into the austenite, or by precipitation as carbide within the bainite plates, or by a combination of both.

The present investigation has revealed a new type of transformation in which the coherent growth of a new phase is probably controlled by a diffusion process. It is suggested that the term ' coherent transformation ' can be used for describing the processes by which both martensite and bainite are formed, in contrast with the incoherent precipitation of ferrite and cementite. The term ' martensite ' should be used for the low-temperature phase when the process is coherent and diffusionless. When the process of coherent growth is controlled by diffusion, the product may be called ' bainite.'

Acknowledgments

This research was carried out in the Metallurgy Department of the University of Birmingham, under the direction of Professor D. Hanson, Director of the

Departments of Metallurgy and Industrial Metallurgy, to whom the authors' gratitude is due for his interest, encouragement, and advice. The authors are also indebted to Professor C. S. Barrett of the University of Chicago for his valuable criticisms, and to Professor A. H. Cottrell, Mr. F. R. N. Nabarro, and many other colleagues for helpful discussions. Their gratitude is also due to the British Iron and Steel Research Association for supplying the grants to carry out this investigation. Thanks are due to Mr. H. W. G. Hignett and Mr. E. H. Bucknall of The Mond Nickel Co., Ltd., Birmingham, to Messrs. Dunford and Elliott, Ltd., and to the English Steel Corporation, Ltd., of Sheffield, for supply of the materials used in the investigation.

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HISTORICAL NOTE NO. 30

Early Use of Mineral Coal in the Chafery of the English Forge

By Dr. H. R. Schubert

THE introduction of the blast-furnace, at the close of the fifteenth century, led to a rapid expansion of the English iron industry during the sixteenth and early seventeenth centuries. At the beginning the fuel used for smelting, refining, and forging was exclusively charcoal. The ever-increasing fuel demands of the industry brought about a shortage of timber trees, which led to reiterated complaints and restrictive legislation, neither of which was able to restrain the constantly growing demand. The idea of using peat or mineral coal as an alternative to charcoal occurred to ironmasters at a very early date. The earliest attempt was made in the Royal Forests of Dartmoor and Exmoor in Devonshire, by one Michael Wynston, who in 1550 obtained a patent for erecting furnaces and forges for the manufacture of iron and steel with coal "*commonly called more cole*," *i.e.*, moor

Manuscript received 15th May, 1952.

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coal, or peat, and without the expenditure of "*any woode or coale of woode*," *i.e.*, charcoal.[1] There is, however, no evidence of any success achieved by this attempt. Wynston probably failed as completely as all the many others who later obtained patents for making iron either completely without charcoal or with an admixture of mineral coal and peat, *e.g.*, Thomas Proctor in Yorkshire in 1589, and Dud Dudley in the seventeenth century. The first to succeed in producing pig iron in the blast-furnace without using charcoal was Abraham Darby, in 1709.[2]

The demand for charcoal was not confined to the blast-furnace ; considerable quantities were also required at the forge for converting pig iron into malleable iron in finery and chafery. It was at the forge that a remedy for saving charcoal by substituting mineral coal was first discovered and successfully applied. This happened shortly before 1600 at one of the ironworks in East Derbyshire, between Derby, Duffield, and Ripley, which the Willoughbys of Wollaton Hall, near Nottingham, had taken over in 1588 for a term of 20 years.[3] A forge belonging to these ironworks was inspected in 1592 by Sylvester Smith, clerk of Sir Francis Willoughby's ironworks at Oakmoor, in Staffordshire. In a letter of 9th October, 1592, Smith reported that he had been told by the workers of the forge "*they are proposed to make iron with sea cole* (*i.e.*, mineral coal) *at the chafery which if they do, will hardly sell above* £10 *the tonne*."[4] The forge referred to was apparently Harthay forge in the parish of Ripley, Derbyshire, as mineral coal was "*employed for the making of iron*" at this forge[5] at about the same time. The reference to a sale price lower than the average implied some doubts of the possibility of successfully forging bar iron with mineral coal. In any case, the attempt was regarded as a novelty by the writer of the letter. This is the earliest evidence in English history of mineral coal being applied in the manufacture of bar or wrought iron, which, however, was confined to the chafery only. It was made possible by a peculiar development in the English process of refining and forging, by which the whole process up to the production of the *ancony*,* *i.e.*, the half-finished bar, was conducted in the finery hearth. Consequently, the process in the chafery was a mere reheating of the ancony ends before they were forged and drawn out by the hammer into the shape of the bar, which the middle of the ancony already had. At this stage mineral coal could be used without detriment to the iron.

The application of mineral coal at the chafery, which, judging by the above-mentioned letter of 1592, started shortly before 1600, is the grain of truth in the constantly repeated claims of the various applicants for patents in the seventeenth century, including Dud Dudley, that they could make bar iron with mineral coal. But even in the chafery, the use of mineral coal was confined to ordinary bar iron ; charcoal was still used with high-quality bar iron, *e.g.*, in the Stour forges in Worcestershire, between 1692 and 1705.[6]

By 1700, application of mineral coal at the chafery appeared to be out of the experimental stage but it was not yet general throughout the country. In South Yorkshire it was given up after a few unsuccessful trials in the forges.[7] On the other hand, ironmasters in South Wales and Monmouthshire had no doubt in this respect. In 1704 John Hanbury, who owned furnaces and forges at and around Pontypool and in the Clydach valley, wrote in his " Observations on the Making of Iron," when all the charcoal was spent at the furnace and the fineries "*we can draw iron out very well with stone coal.*"[8] After this date the use of mineral coal in the chafery soon became more general. There are two references to mineral coal for use in the forge, at Dolobran forge in Montgomeryshire, in 1720.[9] Items "*pit coals for drawing iron*" commenced in 1726 in the accounts of Whittington forge, south west of Stourbridge.[10]

In the finery, however, charcoal still remained the fuel in the eighteenth century. When the French engineer and scientist Gabriel Jars visited the Carron Ironworks in Scotland in 1765, he had been told that mineral coal was used there for refining. He found nothing of the sort, and pointed out that, as in the whole of Europe, charcoal was the only fuel in the finery.[11]

* This term was derived from the French ' encrenée ' meaning ' notched, indented ' and indicating the shape of the half-finished bar, which had a thick square knob at each end whilst the middle already had the smaller shape of the finished bar.

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CORRIGENDUM

Discussion on Hydraulic Power at Appleby-Frodingham

It is regretted that the omission of part of a sentence in Mr. W. M. Hadden's contribution to the discussion on Mr. Lloyd's paper conveys a meaning opposite to that intended by the speaker.

On page 92 of the September (1952) issue of the *Journal*, the words at the top of the right-hand column should read as follows:

" of cylinders are seldom as good as the external surfaces of rams; the machined surfaces of cylinders consequently wear through packings, particularly soft ones, much more quickly than do the better machined surfaces of rams."

IRON AND STEELWORKS ENGINEERING

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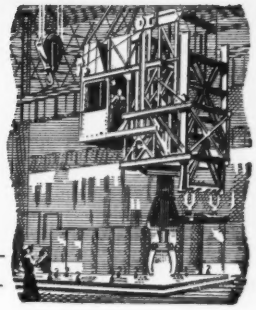

Some Factors Affecting the Operation and Performance of Open-Hearth Furnaces

By D. F. Marshall. B.Sc.(Tech.), Ph.D.

SYNOPSIS

The paper attempts to evaluate some of the more important factors affecting the productivity and fuel consumptions of open-hearth furnaces.

The properties of the principal available fuels are discussed, together with the problems associated with their use in furnaces. For a small fixed furnace the effect of variations in metallurgical load, resulting from changes in the charge constitution, have been determined on a statistical basis. The relationship between slag bulk, rate of production per hour, and fuel consumed per ton was found to be highly significant. Furnace size, as indicated by tapping capacity, has an important effect on productivity and fuel consumption. Some aspects of furnace design, such as hearth size, uptake, and checker design, are discussed. High furnace availability is considered to be an important factor in attaining a high shop and furnace productivity, and the methods adopted to achieve this are outlined. The three avenues of investigation for improving furnace operation and control are combustion-control technique, slag-control practice, and bath temperature measurement. It is suggested that these be considered as closely related factors, with a considerable combined effect in terms of productivity, fuel consumption, and steel quality. The basis for combustion control is discussed in detail, with special reference to the possible advantages to be gained from faster charging.

In this paper an attempt has been made, where possible, to express quantitatively the effect of the more important factors influencing the productivity and the fuel consumptions of open-hearth furnaces. The subject has been considered principally from the plant operator's viewpoint, and many of the points discussed have received additional emphasis by comparing American and British practice. The production of 22 tons of steel per hr. in 200-ton fixed furnaces, for a fuel consumption of 3,000,000 B.Th.U./ton, which represents the best American practice, is an achievement that merits detailed consideration.

TYPES OF FUEL USED

The fuels most frequently used in open-hearth furnaces are natural gas, fuel oil, tar, pitch–tar mixtures, coke-oven gas, producer gas, and coke-oven–blast-furnace gas mixtures.

Properties

Some of the more important properties of open-hearth furnace fuels are given in Table I. All the liquid fuels have decided advantages in storage, distribution, and handling, and with natural gas might be classed as concentrated fuels. They have high calorific values per unit of volume and low waste-gas volumes per unit of heat generated. In a given size of furnace, these properties imply a 20–30% greater potential heat release than is possible with producer gas. They also develop high flame temperatures, and the flames have high heat-radiating properties and high emissivities. Creosote pitch and the tar fuels give flames with emissivities higher than oil-fuel flames and, in this respect, are almost the best available open-hearth fuels. As producer gas is hot and dirty, it is difficult to measure, distribute, and control, whereas the opposite is true of all the other fuels. Continuity of operation is broken by the cleaning out of producer-gas mains, which causes a direct loss in productivity 3–5% greater than with any other fuel. When the differences in flame properties are added to this, producer gas has a productivity value at least 5–10% lower than the liquid fuels and natural gas. Finally, natural gas is almost sulphur-free, and the American oil fuels contain only 0·7–1·0% of sulphur, whereas the British oil fuels contain 2·0–2·5%.

Application

In Britain and America, producer gas was the normal open-hearth furnace fuel until about 1920,

Manuscript received 23rd July, 1952. Dr. Marshall is Assistant Works Superintendent and Fuel and Refractories Officer at the Park Gate Iron and Steel Co., Ltd., Rotherham.

when coke-oven gas and tar firing became popular. Mixed coke-oven and blast-furnace gas fuels were used later in both countries, but more particularly in Britain. From 1932 there was a rapid development in the use of natural gas in America, until, by 1947, about 220,000 miles of pipeline had been laid for both domestic and industrial use. This, together with the extensive development of the fuel-oil industry, almost eliminated producer gas as an open-hearth fuel in America. In 1946–47, fuel oil was introduced on a large scale for the first time in Britain, and eventually about 25% of the total ingot production was from oil-fired furnaces.

These events have had a very marked influence on fuel-using technique and fuel consumptions in open-hearth furnaces. In America the availability of so many excellent fuels, and the necessity to reduce the natural-gas consumption in industry in the winter months, have concentrated attention on the provision of equipment to use two or more of these fuels simultaneously, as is apparent in their burner designs and furnace control systems.[1]

The following combinations of fuels used on five American plants illustrate the wide variations in practice:

Plant No.	Fuel Used
1	50% undiluted natural gas + 50% fuel oil
2	80% undiluted natural gas + 20% fuel oil
3	60% undiluted natural gas + 40% fuel oil
4	4% coke-oven gas + 84% fuel oil + 12% tar. In this case, natural gas diluted with air down to a calorific value of 500 B.Th.U./cu. ft. is frequently used in place of coke-oven gas
5	(*a*) 100% fuel oil (*b*) fuel oil with widely varying mixtures of coke-oven gas (*c*) tar and coke-oven gas mixtures

On the southern and western plants, natural gas in the undiluted state is generally used, but on the central and eastern plants, this fuel is usually let down to about 500 B.Th.U./cu. ft. and used as a substitute for coke-oven gas.

Over 90% of American steel is made from these rich industrial fuels. As they are low in sulphur, high in calorific value, and capable of developing high flame temperatures, they are ideally suited to open-hearth furnaces; they are clean, easy to handle, and lend themselves to the application of accurate measurement and automatic control systems.

In direct contrast to this, only 25% of British ingot production is made in oil-fired furnaces, and this oil has a sulphur content more than twice that of its American equivalent. The remaining 75% is from furnaces using coke-oven gas plus tar, coke-oven gas and blast-furnace gas mixtures, or raw producer gas. The mixed-gas practice is subject to a high incidence of ' foaming ' in the bath, and difficulties of measurement and the lack of continuity due to the cleaning of producer-gas mains have an important bearing on the productivity and fuel consumption differences between the furnaces in the two countries. The conversion of British furnaces from producer gas to fuel oil gave increases in production of about 10%, with similar reductions in fuel consumptions per ton. If the fuel oil used had had a sulphur content similar to Bunker 'C' (0·7–1·0% instead of 2–2·5%) the conversion benefits would have been greater.

Natural gas, liquid fuels, and to a certain extent coke-oven gas, are brought in directly at the ports. As the checkers are required for air regeneration only, furnaces burning these fuels lend themselves to simplified designs, particularly in valve gear, checkers, uptakes, and furnace ends. Despite this, oil-fired furnaces exist with one, two, and even three checker chambers at each end. There is another checker design variation in the form of two-pass checkers, which, in effect, gives double the normal checker depth without deepening the furnace foundations.

The richer fuels are similar, in that the air required for combustion is almost equal to the volume of waste gases formed. They are therefore particularly suited to the practice of working constant air or using only minor variations in flow, because in these cases the air flow almost controls the furnace pressure. Producer-gas and mixed-gas fuels have considerably larger differences between the air required and the waste-gas volumes produced, and they are not readily adapted to this simplified operating technique. Coke-oven gas used with liquid fuels or with blast-furnace

Table I

SOME PROPERTIES OF FUELS USED IN OPEN-HEARTH FURNACES

Details of Fuels Used	Gross Calorific Value	Theoretical Air Required	Volume of Waste Gases	Air Required per Therm Generated, cu. ft.	Waste Gases per Therm Generated, cu. ft.	Theoretical Flame Temp., °C.
Gaseous Fuels	B.Th.U./cu. ft.	cu. ft./cu. ft. of gas	cu. ft./cu. ft. of gas			
Natural gas	1100	10·58	11·73	906	1060	1970
Coke-oven gas	525	4·57	5·27	875	1010	2030
Producer gas	160	1·28	2·08	800	1300	1590
Blast-furnace gas	92	0·69	1·55	750	1680	1480
Mixed coke-oven and blast-furnace gas	236	1·98	2·79	839	1180	1650
Liquid Fuels	B.Th.U./lb.	cu. ft./lb.	cu. ft./lb.			
Fuel oil—British	18,700	184	194	985	1040	2080
Fuel oil—American	18,800	180	190	956	1010	2100
Creosote–pitch	16,100	157	165	975	1025	1940

gas comes into both categories. Producer gas is probably the most difficult of all the fuels to handle. It is difficult to measure and distribute and, within the furnace, it presents special problems in the maintenance of the original port shape throughout the furnace campaign. In this connection, the experimental work on flow patterns in small models[2] and the measurement of heat transfer from flames developed when using various types of port in a working model furnace[3] are of particular importance. This variation of port size, and hence in flame shape, has also made it difficult to apply roof-temperature measurement to producer-gas-fired furnaces. The successful development of automatic roof-temperature control on a producer-gas-fired furnace,[4] followed later by an automatically operated gas producer at one British plant, represents a marked advance in producer-gas-firing technique.

In the majority of cases, liquid fuels are applied through a single burner passing through a dog-house type of construction or mounted directly in the gable end of the furnace. Certain variations of this arrangement are now being successfully applied. At one plant, the burner tip is divided into three rows of jets, each having three small apertures, the total area of the nine apertures being similar to that used with the single-jet burners. Another variation consists of mounting two smaller burners in the end wall at about 5 ft. 9 in. centres, and using both burners throughout the charge. Twin burners mounted in the same vertical plane are also being used, and one furnace uses triple burners with the third burner mounted centrally about 9 in. higher than the two lower burners at 5 ft. 9 in. centres. Where charging facilities are good, an increased productivity of about one ton/hr. on 200-ton furnaces is claimed for these multi-jet or twin- and triple-burner constructions; *i.e.*, an increase of about 5%.

Another interesting liquid-fuel application is that used by the Royal Netherlands Steelworks, in which one burner is mounted in each of the four splay walls in front of the single uptake at each end. For this arrangement, it is claimed that the peak emissivity developed by the flame is transferred from the throat of the furnace to the bath proper.

Experience has established that tar and pitch fuels give better results in the furnace than fuel oil, but they give rise to heavy dust deposits in checkers and exit flue systems.[5] This phenomenon, which has not yet been satisfactorily explained, is more pronounced in cold-charged furnaces than in hot-metal shops, and its effect is less serious where coke-oven gas and tar are used together. As it is essential to have separate circulating systems for tar and oil fuel, the method of using tar in American shops seems to be to build two separate circulating systems up to the burners of each furnace, and then to use tar on a group of furnaces for a period, at the end of which this group is put back on to oil fuel and a second group of furnaces is changed over to tar. By this means, the deposits in checkers and flues are kept within reasonable limits.

Direct comparisons between coke-oven gas and fuel oil are not possible, but American trials have suggested that, when mixed fuels are used, not more than 35% of the heat input should be in the form of a gaseous fuel if maximum melting rates are to be achieved. The proportion of coke-oven gas used with tar or fuel oil is usually below 30%, but tests made up to values of 45% showed that, above 30%, output rates fall and fuel consumptions increase. Recently, efforts have been made to overcome this by using coke-oven gas at higher pressures (30 lb./sq. in.) on these mixed-fuel plants.

Natural gas is piped to plants at 250 lb./sq. in., and the pressure is reduced to 50 lb./sq. in. for local distribution within the works. On the plants where the gas is used in its richest form—*i.e.*, 1050 B.Th.U./cu. ft.—it is applied with oil fuel through combination burners. In some cases, the gas and oil are passed down separate pipes mounted in a water-cooler burner; in others, the oil pipe is mounted centrally in the gas pipe and the two fuels meet at about 6–8 in. from the tip of the burner. In one instance, early flame development is encouraged by passing the gas through a jet into a central tube and using an injector effect to induce a limited amount of cold air with the gas stream.

At other plants, the calorific value of the natural gas is controlled in an automatically operated mixer, giving a product of about 500 B.Th.U./cu. ft., and is then distributed for use in much the same way as coke-oven gas.

The technique of handling fuel oil or tar seems to have resolved itself into a standard pattern. The question of storage depends largely on local and national supply conditions. Bulk heating of the circulating fuel up to atomizing temperature has, in general, proved more efficient than heating at the individual consuming units, and is often automatically controlled. The importance of accurate measurement of fuel flow cannot be overstressed, and in many cases this is greatly facilitated by atomizing at a constant viscosity rather than at a constant temperature. Although, for most systems, electrically operated centrifugal or gear pumps have the advantage of eliminating pulsations, especially on small-capacity systems, it has been shown, particularly in Britain, that reciprocating pumps fitted with an adequate air vessel can give satisfactory results.

The method of construction of atomizers for open-hearth furnaces is not now considered to affect vitally either fuel consumption or productivity. The vast amount of experimental work on both British and American furnaces, coupled with the large-scale model-furnace tests on flame radiation and emissivity, have suggested that the quantity, condition, and pressure of the atomizing agent are more important than the atomizer design. In several British plants it has been established that by increasing the steam/oil ratio, particularly during charging, faster times and lower fuel consumption are achieved. At Park Gate, improvements were noted up to ratios 9 lb. of steam per gallon of oil. It is also significant that the steam lines originally put down on British plants were based on the use of ratios similar to those in America, and in many cases these have since been replaced by larger-capacity systems.

There appears to be an indication that higher steam/oil ratios are required on the smaller furnaces, but

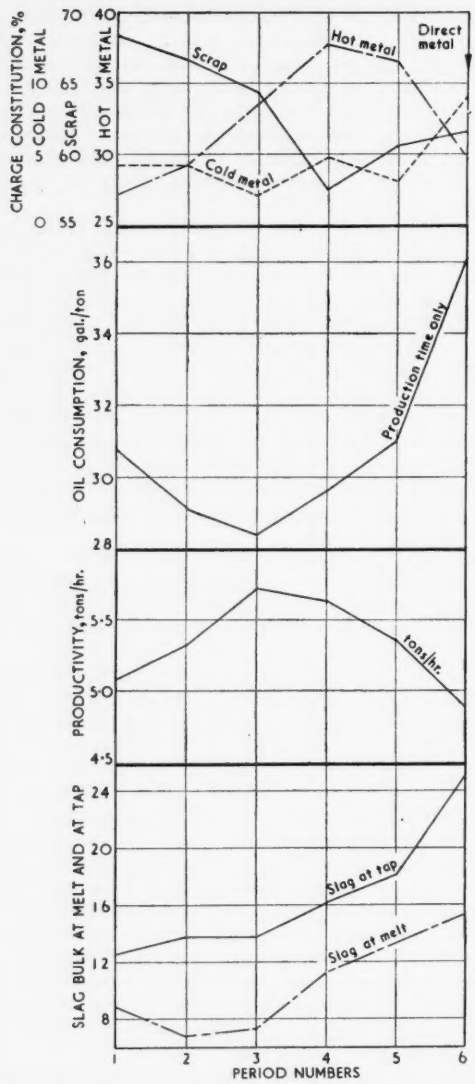

Fig. 1—Average shop operating data for various charges

on the larger units, where air and fuel have more chance to mix between burner and bath, lower ratios give satisfactory results. The problem of atomization seems to be resolving itself into a question of energy available at the burner nozzle, and it is probable that the high steam/oil ratios now being used may eventually be reduced by considering the condition of the steam and applying a greater degree of superheat. In American plants, the steam is frequently obtained from high-pressure generation systems and is well superheated, whereas many of the smaller British plants use steam in the lower-pressure ranges, about 150 lb./sq. in., with relatively little superheat. This, together with the large size of American furnaces, may account for the lower steam/oil ratios used in that country. Another method of reducing the large quantities of steam required lies in improving the chances of entraining combustion air rapidly. For example, in a recent conversion of a Park Gate furnace to a central-uptake design, one of the first operating changes noted was that the steam/oil ratios required were lower than on the previous builds.

CHARGE CONSTITUTION

During the past 12 months, fluctuations in scrap supplies have been of such a nature that many plants have been compelled to vary charge constitutions to an unprecedented degree. Although these conditions have generally had an adverse effect on the productivity and fuel economy of individual plants, they have afforded an opportunity to assess the effect of raw-material supplies on output and fuel consumption. Figure 1 illustrates this point. It has been compiled from an analysis of the operating conditions of the Park Gate open-hearth shop over a period of three years. From the weekly records of the shop, periods have been selected during which conditions were reasonably stable yet at different levels within the respective periods. All the charges made within each period are included. This diagram gives a clear indication of an optimum charge constitution containing 65% of scrap, 33% of hot metal, and 2% of cold metal. When the hot-metal proportions are decreased, the charging and tap-to-tap times and the fuel consumption per ton are increased, and the rate of output per hour is decreased. Similarly, when the hot metal is increased beyond this optimum, the charging time is not materially reduced and the melting and refining times are extended. The slag-bulk curve shows that the slag bulk in the high-scrap side of the optimum conditions does not vary much, lying within the range 12·5–13·6%, and the advantages gained when the optimum charge constitution is approached are mainly due to charging less cold material. On the high hot-metal side of the optimum, the factor of increasing slag bulk predominates. The feed charged to meet the slag requirements prevents any major reduction in charging time, which might be expected from the decreasing scrap proportions, and the higher slag burden extends the melting, refining, and fettling times, so that there is a marked increase in the fuel consumption per ton. The normal Park Gate pig-iron analysis, working on home ores, is 1·2% Si, 1·3% P, 0·05% S, 1·2% Mn. During the first period, when a small quantity of foreign ore was used in the burden, the analysis was 1·05% Si, 1·0% P, 0·06% S, 1·5% Mn. Normally, all the hot metal was treated in an active 400-ton mixer, reducing the silicon content to 0·3–0·5% and the phosphorus content to about 1·2%. The additional wear and tear on the mixer, due to increasing the metal throughput by about 30% and also to a deterioration in pig-iron analysis when using home ores, increased the amount of fettling required and, consequently, the number of occasions when direct metal had to be used on the furnaces. Period 6 represents operation under these extreme conditions, when slag bulks of 25–27% were carried, causing adverse trends in productivity and fuel consumptions. This investigation has been further supplemented by a statistical analysis of 51 weeks' operation of the shop. The correlation coefficients of six variables related to productivity, in tons/hr.,

taken over a period of 51 weeks, are given in Table II.

Sulphur was not examined in this way, but its effect, with that of silicon and phosphorus, comes into the slag-bulk coefficient and the fuel-consumption coefficient. These two highly significant relationships, which are shown in Figs. 2*a* and *b*, are of particular importance in comparing practice between plants or even in different countries. The diagrams also emphasize the advantages of conditioning pig iron by modifying blast-furnace practice, by mixers, by desiliconizing in ladles with scale or oxygen, or by Bessemer converters, before it is charged in open-hearth furnaces. Whatever means are adopted to improve the quality of the pig iron, it is still vitally important that the slag bulk is kept as low as possible by practising a slag-control technique suited to the particular plant conditions. The development of such a technique, which has proved to be extremely beneficial to the Park Gate furnace operation, has been described by Fairley.[6]

In Britain there is greater variation in practice affecting metallurgical load; consequently, any statistical analyses of plant or furnace performance tend to show a wide scatter. In American practice, conditions are almost exactly the reverse. Pig iron is almost universally low in sulphur and phosphorus, and slag bulks vary between 7 and 10%, being on the average less than half those carried in British practice, with consequent advantages in productivity and fuel consumptions. Finally, whereas the high phosphoric irons of Britain and Germany show optimum values in charge constitutions, no similar effect is shown in America. Greene[7] made a study of the effect of burden constitution on open-hearth furnaces at an American plant where the normal hot-metal analysis was 0·9% Si, 0·34% P, 0·035% S, 0·25% Mn. In this case, when the hot-metal content was increased from 20% to 55%, progressive increases in productivity from 17 to 20·4 (short) tons/hr. were shown. Within the ranges of hot-metal content examined, there was no optimum charge constitution as shown for British practice, but it is interesting to note that on scrap heats (*i.e.*, 30–41% hot metal), higher silicon content in the pig iron gives slightly increased productivity, whereas in the ore heats (43–53% hot metal), where the silicon is carried off in a flushing slag, the reverse was found.

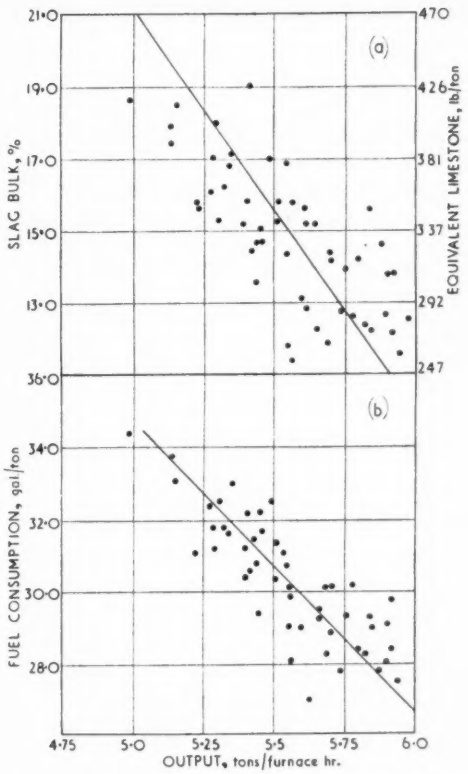

Fig. 2—Relationships between productivity and (*a*) slag bulk, (*b*) fuel consumption

Table II

RELATIONSHIP BETWEEN SIX FACTORS AND PRODUCTIVITY

Variable	Correlation Coefficient	Level of Significance	Comment
Total acids in melting slag	0·160	...	Not significant
P charged, lb./ton ingot	−0·588	0·001	Significant
Si charged, lb./ton ingot	−0·512	0·001	Significant
P + Si charged, lb./ton ingot	−0·580	0·001	Significant
Equivalent limestone, tons/ton ingot	−0·720	0·001	Very significant
Oil, gal./ton ingot	−0·831	0·001	Very significant

FURNACE SIZE

Furnace size, as measured by tapping capacity, affects both the fuel consumption per ton of ingots made and the productivity measured in tons produced per hour. Quantitative estimation of the furnace-size effect is extremely difficult in British practice, where statistical analysis of plant operation shows a very wide spread, due principally to heavier and more variable metallurgical loads in the fixed hot-metal furnaces, to a greater proportion of tilting-furnace practice, and to a wide variation in charging facilities. In American practice, excluding that in the Alabama district, metallurgical loads are much more uniform and at a considerably lower level than in British practice; charging facilities are, on the average, better, and tilting practice is not extensively used.

In Fig. 3, data obtained from 15 American plants have been plotted. Each point represents the operation of a complete shop. Although Fig. 3 may have statistical limitations, it covers a very wide range of furnace size, and the plants included represent a good cross-section of American practice. The four plants with 200-ton furnaces, which show lower fuel consumptions and higher productivities than the trends

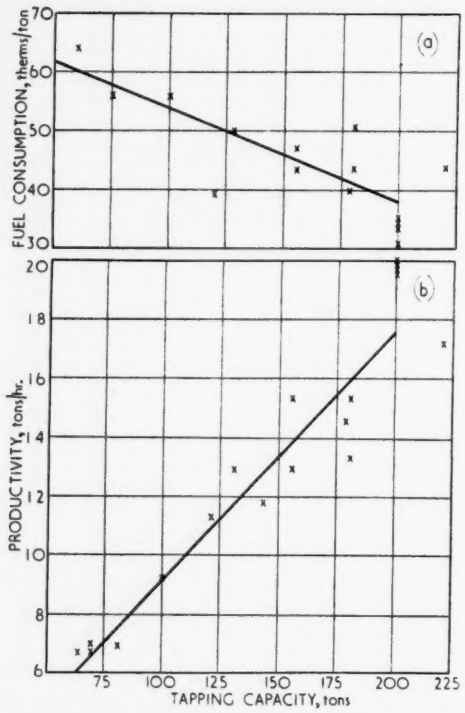

Fig. 3—Influence of furnace size on (*a*) fuel consumption and (*b*) productivity

shown by the other plants, are all new plants, well laid-out and having excellent charging facilities.

The latest British plant, using 200-ton furnaces and having a metallurgical load lower than that used in the average British practice, compares very favourably with these four new American plants. The large British tilters also fall into a similar category. Figure 3 indicates that an increase in furnace size from 100 to 200 tons will lower the fuel consumption by about 15 therms/ton and increase productivity by 6–9 tons/hr. The position of these four newer plants suggests that where plant layouts and charging facilities are adequate to meet the requirements of the larger furnaces, the change in capacity from 100 to 200 tons might reduce fuel consumption by as much as 20 therms/ton, with a productivity increase of about 10 tons/hr.

Considering British practice in relation to Fig. 3, there is, on the average, an increased fuel consumption of about 15% for cold-charging practice and about 20% for hot-metal practice. American furnaces show an increase in productivity of 0·069 tons/hr. per ton increase in tapping capacity, and a recent survey gave the corresponding value for British hot-metal furnaces as 0·027 tons/hr. for each ton of added tapping capacity.

In a discussion[8] on the effect of metallurgical load on open-hearth furnace operation in Britain, it was shown that whereas 80-ton cold-charged furnaces showed a marked deterioration in performance as the metallurgical load increased when higher proportions of cold pig were used, 100-ton and 150-ton furnaces showed a progressively smaller falling-off in performance under the same conditions. In this instance, it was considered that the larger furnaces were able to make up in the charging period some of the time lost in the later stages of the charge.

FURNACE DESIGN

Most of the design features that are considered to influence furnace operation are constantly being tested on a large scale on individual plants. In recent years, this type of plant experiment has received a welcome stimulus from tests on both cold and hot scaled-down models; in particular, the aerodynamical studies made on a laboratory scale model by Chesters and his collaborators[9] have been sufficiently fruitful to merit the construction of a furnace along the lines suggested by this model. Another excellent example of this type of scaling-up of small model tests is the work on port and uptake design and roof contour on a small working model, reported by Allen[3] and connected with the actual furnace experience reported by Hall and Leckie.[10] There is a further connecting link in the research on flame radiation, which is being conducted in a very much larger model furnace in Holland[11] and supplemented by factorial trials on operating furnaces. There is already some indication that these techniques will have considerable influence on furnace design and operation.

The influence of furnace size, as measured by tapping capacity, to some extent shows the overall effect of a large number of separate design factors. This is particularly true in the case of the very large furnaces, usually associated with newer plants, where each component part of the furnaces has been designed for a specific duty. In many of the older plants using smaller furnaces, the need for expanding output within existing layouts has resulted in many departures from accepted design standards, especially in the frequent adoption of deep baths.

For a given tapping capacity, the most critical factor affecting furnace performance is the shape of the bath in terms of area and depth. Figure 4 summarizes the results of a recent survey of American furnaces, which substantiates their expressed preference for large shallow baths. When the hearth area per ton of tapping capacity on a 200-ton furnace is decreased by about 0·56 sq. ft./ton capacity, the productivity is reduced by 29%, and by 27·5% for a similar change on a 100-ton furnace. The bath area per ton of capacity is similar for fixed furnaces in both American and British practice, but baths are somewhat deeper in Britain, where the furnaces carry about double the slag thickness. In most American plants the bath depth is artificially increased by extensive building-up with dolomite on the sill plates. When all other design factors are equal, deeper baths either reduce productivity or increase the fuel consumption per ton. The example previously quoted, regarding the advantages of larger furnaces when handling increased metallurgical load, supports the American statistical analysis shown in Fig. 4. In both cases, increases in hearth area are generally associated with larger doors and greatly improved

charging facilities, which directly affect both productivity and fuel consumption.

The influence of combustion-chamber volume is not easy to determine, as it is an integral part of the size factor. A statistical analysis of British furnaces has shown that, for a constant tapping capacity, each 100 cu. ft. increase in combustion-chamber volume increases productivity by 0·087 tons/hr. on cold-charged furnaces, whereas on hot-metal furnaces—taking combustion volume as a measure of furnace size—each increase of 100 cu. ft. reduces the fuel consumption by 0·39 therm/ton.

Uptakes and Slag Pockets

The uptakes and slag pockets are usually sufficiently large to remove the waste gases and collect the slag, and it is now becoming increasingly evident that they also have an important effect on combustion at the incoming end of the furnace. More gas- and oil-fired furnaces are now being built with a single central uptake in place of the two normally used on conventional designs. On the smaller British furnaces, this principle has been adopted with considerable advantage[10]; there has invariably been an increase in production and—almost equally important—a reduction in the sulphur content at melting. The introduction of basic ends on to American furnaces led to the adoption of similar construction, but with a very large uptake. In these furnaces, the changes gave slightly lower output rates with higher fuel consumptions. These large single uptakes have in some cases been replaced by smaller uptakes, but the results have not yet been as good as those obtained on the smaller British furnaces. The critical effect of the uptake size and shape has already been established in the British trials, but it seems probable that the single-uptake principle has a better application in the smaller furnaces. It is noteworthy that, although fuel rates vary *pro rata* with the size of furnace, the larger furnaces, in which the distance from the bath to the actual chambers is greatest, invariably give better checker lives than the small furnaces. This result is due to the more efficient removal of slag particles in the longer path of travel on the large furnaces and to the important part that

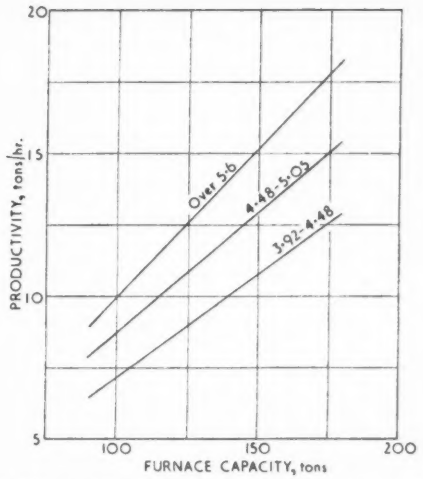

Fig. 4—Effect of hearth area on production rate. Values on curves show hearth area (sq. ft.) per ton of furnace capacity

uptakes and flues play in the heat-exchange system of a furnace. In terms of checker life, this effect can vary from the almost unlimited checker life on the large modern 200-ton fixed furnaces to as low as 20–30 weeks on the older 50/70-ton fixed furnaces with limited slag-pocket capacities. Although this factor may not have a serious effect on productivity per furnace per hour, it has an important effect on the productivity of a complete shop, because it influences the availability of a furnace over a long period of time. The shorter the checker life, the more difficult it becomes to plan repairs. In general, refractory consumption also increases, for it becomes increasingly difficult to match up the effective lives of the different parts of the furnace, such as roofs, linings, and checkers.

Checker design and performance are frequently considered in relation to the operation of open-hearth furnaces, and in recent years the important part played by slag pockets and uptakes in heat transfer

Table III
CHECKER TRIAL DATA

Furnace	A		C			E	
Checker Filling	1	3	1	2	4	1	4
Total heating surface on one end, sq. ft.	7040	6090	4900	3920	4680	7940	7222
Free area on one end, sq. ft.	71	81	53	67·5	53	79	79
Tons/hr.*	6·33	6·08	5·99	5·30	5·09	5·90	5·61
Tons/hr.	5·35	5·15	5·25	4·67	4·40	5·10	4·87
Fuel, gal./ton	30·3	31·2	29·1	34·0	36·8	39·4	33·2
Waste gas, inlet to boiler, temp., °C.	490	543	595	635	715	530	594

* Excluding fettling

1. 6 in. × 6 in. pigeon hole from 9 in. × 4½ in. × 3 in. bricks
2. 9 in. × 9 in. solid flue from 9 in. × 4½ in. × 3 in. bricks
3. 7½ in. × 7½ in. solid flue from 9 in. × 4½ in. × 3 in. bricks
4. 6 in. × 6 in. solid flue from 15 in. × 6 in. × 3 in. bricks

has also been emphasized. In British practice, where many of the smaller-capacity furnaces have larger hearths built on original checker structures—and might therefore be considered short of checker capacity—the question of checker construction is vital. Excluding special shapes, two classes of checker construction are used: the pigeon-hole type, which seems to be more efficient thermally but not too stable mechanically, and which is prone to deterioration due to fouling of the apertures, especially when built in the staggered form; and the solid-flue type, which is much sounder mechanically but not so efficient thermally. The smaller the furnace, and, in particular, the smaller the slag-pocket capacity, the more vital do these considerations become in regard to furnace performance. Some indication of this is given by the results of tests made on three Park Gate furnaces, in which the checker-chamber capacity was not varied but different checker constructions were used within the limits of that capacity.

The total volumes of checker fillings at one end of each furnace were: *A* furnace 1860 cu. ft., *C* furnace 1305 cu. ft., and *E* furnace 2069 cu. ft. The characteristics of the filled chambers, and a summary of the operating results for furnaces with various fillings, are given in Table III.

These figures, which summarize the results from a full campaign for each type of checker, indicate that the pigeon-hole construction is thermally more efficient than the various types of solid-flue construction. In all cases, the output per hour was greater, the fuel consumption per ton of ingots was lower, and the average waste-gas temperature was lower. However, this construction proved very liable to distortion, especially on the top courses, whereas the solid-flue types were mechanically stronger and retained the original formation throughout their life, but were inclined to give trouble due to blocking of the flues immediately below the checkers. The results suggest that for these three furnaces the solid-flue types would have to have greater depth than the pigeon-hole type, to give thermal efficiencies approaching those obtained with the latter construction.

It will be appreciated that these tests were made on furnaces with a very short distance between port and checkers and where the provision of adequate slag-pocket capacity was difficult. The results emphasize that, in such conditions, changes in checker design can have a very critical effect on furnace performance. With larger furnaces there seems to be more latitude in checker construction, but it is interesting to note that on American furnaces, where solid-flue-type construction is more common with openings up to 9 in. square, a minimum checker depth of 15 ft. is now considered desirable. The two-pass checker system, which is used on some of the most highly productive furnaces known, gives an effective checker depth of about 30 ft.

FURNACE AVAILABILITY

Productivity is usually expressed in terms of output per man per year. It is equally important, when comparing outputs on either a plant or a national basis, to consider the degree of furnace availability. This can materially alter the total output from a given plant without affecting output per man per hour. One of the outstanding characteristics of American practice is the high availability attained, which, at a national average figure of 91%, is about 10% higher than that in Britain. This difference arises from a number of interrelated factors.

In plant layout and furnace design, every consideration is given to making furnaces readily accessible to the equipment for transporting bricks, constructional items, and debris. In the most critical parts—such as slag pockets—bulldozers and traxcavators can be driven directly into the pocket, and equipment is also provided to use pit-road cranes for work in these parts. In many plants, both railway and road vehicles can be driven up to the furnace, and high-lift and fork-lift vehicles have a free run from stockyard to furnace.

Certain devices are used to promote longer life and reduce the incidence of stops for minor repairs. Among these are the use of basic furnace ends, the extensive water-cooling of the superstructure, and, in particular, the use of archless water-cooled door frames. Checkers are frequently cleaned by steam lances, and steam soot blowers are fitted beneath checkers and in flues. Frequent use is made of B.R.I. guns for lining and burner-block maintenance, and in some cases for filling up holes in the hearth. With a few exceptions, fettling machines are invariably used on the smaller furnaces. These are propelled by diesel engine, by petrol engine with controls on three sides, or electrically, by using a trailing cable.

When a furnace actually comes off for repair, the matter is treated by all concerned as a vital task. As in British practice, repairs are made to a planned schedule based on the estimated life of the various parts, and this is supplemented by regular inspection. In some cases slag is removed in one piece, using the casting-bay cranes or bulldozers within eight hours of taking off the oil. Other plants drill the slag (in one case, before the last charge is tapped) and break it up by firing explosive charges, removing the debris with bulldozers or traxcavators. Special rakes are also fitted to pit-road cranes to pull debris out of the pockets. Roofs are usually dismantled into pans on the hearth, but considerable quantities of debris are removed *via* the up takes and traxcavators below.

When bricklaying starts, every known mechanical aid is applied. Bricks are delivered in palletized form in trucks, and are picked up by cranes or high-lift trucks and transported up to the bricklaying point. On the higher portions of the structure, light platforms are clipped to the binding, to give working space and storage room. Compressed air or electrically operated elevators or high-lift trucks supply material to these platforms. Flues are now being cleaned by vacuum-dust extractors, and compressed-air lifts are used for handling material in and out. On the best-equipped plants, quick roofs take 24–48 hr. oil-off to oil-on, and a repair, including new roof, linings, uptakes, slag removal, and checker cleaning, is done in four days oil-off to oil-on. On plants with less of these facilities, times extend to seven days for a general repair. For emergency hot-roof repairs, speed of operation is increased by simple hangers with steel

formers, sometimes with interlocking bricks; for example, a 50-ft. length of crown may be replaced in 4 hr. The first three general repairs on a 500-ton fixed furnace took 74, 100, and 65 hr., respectively, oil-off to oil-on.

Furnaces are brought back into operation with remarkable speed after repairs are completed. In most cases the furnace is charged with very heavy scrap as soon as the hearth is clear. The roof is then put on, and probably begins to warm up as the other repairs are continuing. Wood fires or coke-oven flares on the hearth are used in the early stages, followed by coke-oven gas and later oil on the burners. The time taken from starting to heat-up to tapping the first charge may be as low as 24 hr., and is usually between 24 and 40 hr. The whole question is approached primarily from a production aspect, with the consideration of damages to refractories taking second place.

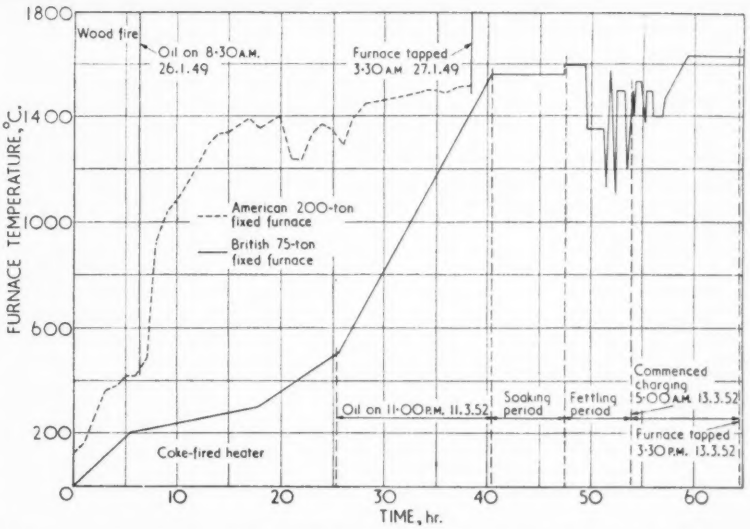

Fig. 5—Comparison of typical British and American heating-up schedules

In Fig. 5 an American furnace heating-up curve is compared with typical British practice. Where efforts to shorten these times are made on British furnaces, they almost invariably result in heavy spalling of the brickwork, with a consequent reduction in roof life. It seems, therefore, that more liberties can be taken on the larger-size furnaces with driven fuels than on the smaller units.

FURNACE OPERATION AND CONTROL

Furnace operation is being improved by investigations proceeding along three main lines: combustion control, slag control, and bath temperature control. Although reference will be made principally to the first of these, the other two are very closely related to combustion control and have a direct effect on fuel consumption and productivity. The important effect that slag bulk has on open-hearth furnace performance amply justifies the effort required to apply a control technique to ensure that slag bulks will be as low as possible. Closely allied to the combustion and slag controls is the control of the temperature of steel in the bath. In Britain more than in America the difficulties of application of bath-temperature measuring equipment have been largely overcome, and very important advantages in terms of improved steel quality have resulted from such application. At Park Gate, for example, starting with equipment for experimental use on one furnace, the technique was rapidly extended so that, within six months, three or four immersions were being made on every cast on the nine furnaces in the shop. For the past two years the equipment has operated at the rate of 16,000 immersions per year, with beneficial results in the quality of the steel made. The tapping temperatures aimed at for the various steel qualities are given in Table IV. For any particular quality, the accepted temperature range is 10° C. The preparation of the thermocouples and checking and calibration of the equipment is arranged on a 24 hr. per day basis, along the lines already described[12] for the control of furnaces.

Table IV
BATH TEMPERATURE CONTROL

Specification	Tapping Temperature, ° C.	
	Direct Casting	Uphill Casting
Alloy and Carbon Steels		
C 0·10/0·20	...	1590/1600
C 0·21/0·30	...	1585/1595
C 0·31/0·40	...	1580/1590
C 0·41/0·50	...	1575/1585
C 0·51/0·60	...	1570/1580
C 0·61/0·70	...	1565/1575
C 0·71/0·80	...	1560/1570
Carbon Steels		
C 0·10/0·15	1580/1590	...
C 0·16/0·25	1575/1585	...
C 0·26/0·40	1570/1580	...
C 0·41/0·50	1565/1575	...
Free Cutting	1600/1610	...
Rimming Steel	(Before additions)	
C 0·08/0·1	1600/1610	...
Grain-Size Qualities	Temperature as for C range + 10°	

Furnace operating technique and methods of combustion control, both manual and automatic in type, have received considerable attention in recent years. In Britain especially, considerable progress has been made towards the ultimate ideal of a fully automatic open-hearth furnace. The process has meant an ever-increasing degree of measurement of all the factors involved in furnace operation, coupled with the introduction of changes based on these measurements. This has led directly to improvements in

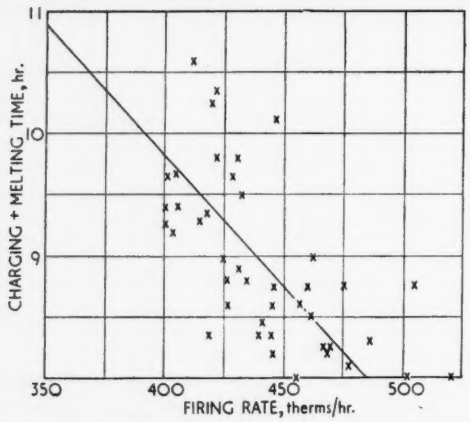

Fig. 6—Effect of firing rate on charging and melting time

furnace operation beneficial to both productivity and fuel consumption, and to modifications in furnace design. In many cases, by emphasizing suspected bottlenecks in the supply position, it has brought about improvements in shop layouts to overcome these difficulties. Also, added impetus has been given to the development of instrumental equipment capable of withstanding the arduous open-hearth shop conditions.

The performance of a furnace is greatly influenced by the manner in which it is charged. Good charging practice depends on two things: the availability of scrap at the right time and in the right quantities, and the efficient burning of the maximum amount of fuel. The former is a function of plant layout and the scrap-handling facilities provided, and the latter is primarily a question of furnace operation. Some indication of the general relationship between these factors and the fuel consumption per ton of ingots is given in the following furnace operating data.

Tons of Scrap Charged/hr.	Maximum Heat Input, therms/hr.	Fuel Consumption, therms/ton ingots
87	830	35
72	750	40
50	750	31
24	650	50
15	550	56

The lower fuel consumptions are associated with the high scrap-charging rates and high fuel inputs. The high charging rates are associated with large furnaces, where the charging facilities are usually better than on the older plants with smaller furnaces. The rate of furnace charging is equally important when the furnace size factor is eliminated by considering results on individual furnaces.

Fayles,[13] when discussing the effect of scrap charging on a cold-charged furnace operating on 25% of cold pig and 75% of scrap, showed that, by reducing the scrap-charging time from 320 to 235 min., the output rate was increased from 7·35 to 8·31 tons/hr. On hot-metal furnaces the relationship between charging time and output rate was shown to be equally important.

Robertson and Thring,[14] when examining the performance of an 80-ton oil-fired open-hearth furnace, showed that there was a significant relationship (Fig. 6) between the fuel input and the charging and melting time, which could be expressed as follows: charging + melting (hr.) = 18·46 − 0·026 (hourly heat input in therms).

Once a furnace is charged, fuel inputs are controlled by the limitations of the furnace refractories, and—except for the use of basic refractories—there is little scope for faster working which can result from fuel application only. It is therefore vitally important that every advantage is taken of conditions during charging, when the cooling effect of the scrap can be used to keep the refractories within safe temperature limits.

In America, the optimum fuel flow has been fixed in a somewhat empirical way, based partly on furnace size and partly on operating experience checked by waste-gas analyses. Figure 7 shows the relationship between hourly fuel input during charging and bath length observed for the group of furnaces. There is a reasonable correlation between these two variables.

British charging technique has developed along similar lines, but the successful application of roof-temperature measurement has had two important additional effects upon furnace operation. Where the roof temperature has never reached the maximum safe-working temperature during charging, investigations have been made as to whether the furnace could efficiently carry a higher thermal input. Conversely, where, on occasion, the roof temperature has been at the limit and the fuel flow has been reduced, scrap supply difficulties have been emphasized. At Park Gate the problem has been taken a stage further, by attempting to calibrate furnaces in terms of maximum air flow, with the exhausting system wide open, and then applying a calculated maximum possible oil flow subsequently checked by gas analysis. Even with known furnace design limitations, this has ensured a maximum driving rate during charging.

During melting and refining, the British practice of working to a fixed roof temperature again ensures maximum heat inputs, whereas the American system of preset flows does not necessarily do this.

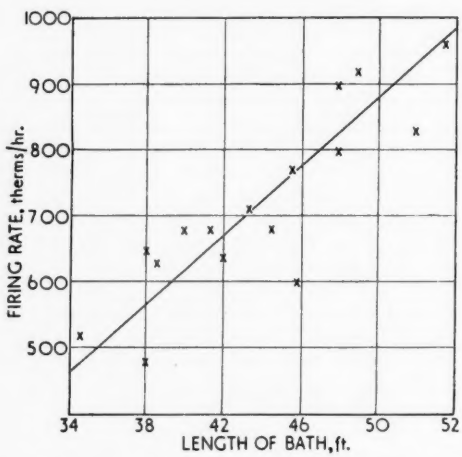

Fig. 7—Effect of bath length on firing rate during charging

The contamination of siting apertures by the use of large fettling machines has undoubtedly affected the development of roof-temperature measurement and its use as a control medium in American furnaces. On the other hand, material benefit has been obtained from the extensive use of valves and equipment, which act as flow setters and stabilizers. As with the British automatic control systems, these have greatly reduced the number of changes in flow that need to be made by the operator. It is probable that the combination of the two systems—*i.e.*, a British roof-temperature controller operating through an American type of flow-stabilizing valve—would result in a system of control superior to anything now seen in either country.

Automatic Control

The rapid development of various types of automatic controls in recent years shows that most of the initial difficulties have been solved, and that material benefits in productivity and fuel economy are being obtained. It has also been shown[12] that, even on hand-controlled furnaces, a systematic method of furnace-operating technique can give considerable benefits. Some of the modern methods of automatic control have been based on the experience gained on systematic hand operation. The principal methods of control adopted are as follows:

Fuel Flow—In British practice, particularly on fuel oil but also with mixed gas and with producer gas, the measurement of roof temperature is being made the controlling medium. On hand control, recorded roof temperatures are made the basis of changes in fuel flow, and on automatic control there is invariably a direct mechanical and electrical linkage between these two. The safe-working upper limits of roof temperature, as measured by total radiation pyrometers, are usually fixed between 1630° and 1650° C. In nearly all cases, there is a fixed maximum fuel flow applied during the charging period, after which the roof-temperature measurement takes over the full control. On oil-fired furnaces, automatic steam/oil regulation, when applied, is based on flow measurement; and on hand-controlled furnaces, predetermined ratios are applied, particularly in the charging periods.

In American practice, the ability to consume two or more fuels individually or in combination, at any time, is the predominant feature of nearly all furnace-control systems, and the flows required are controlled through stabilizing valves. The flows to be applied are fixed for the various parts of the charge and set through a hand-wheel, so that, whatever else the controls do, the furnaces are fundamentally hand-controlled. The hand-wheel is usually calibrated in terms of total heat input, B.Th.U./hr., and, in effect, it varies the air loading transmitted to the remainder of the control system. By balancing this air-loading system against a second system set up by the measurement of liquid fuel and gaseous fuel, a variety of control arrangements is possible, such as:

(*a*) The application of a fixed oil/gas ratio at all levels of total heat input.

(*b*) The maintenance of the heat input from gaseous fuels at a constant level, the required total B.Th.U. input being supplemented with liquid fuel. If the gas flow fails, the shortage is made up with liquid fuel.

The same system is capable of giving either automatic fuel/air control or constant air-flow control, whatever proportions of the two fuels are used. Where liquid fuels are used, steam/oil ratio regulation, when applied, is based on pressure and not on volume as in Britain. The danger in this method is that, if a valve or atomizer becomes choked with dirt, the whole of the steam side is out of balance with the oil side.

Air Flow—Although, generally, fuel/air ratio control is more prevalent in Britain, the conversion to fuel oil has directed attention to the use of constant air flow or of two-level air flow throughout a charge, especially on hot-metal furnaces. The recent work done by B.I.S.R.A. on air infiltration, and the results obtained on furnaces employing one- or two-level air flow, indicate that this departure from theoretical standards is not entirely without justification. In America, after many plants had been installed with expensive equipment for automatically controlling fuel/air ratio, there was a widespread reversion to the use of constant air flow or of limited air-flow changes. On mill furnaces, where the construction adopted does not result in air infiltration as much as on open-hearth furnaces, there is almost universal agreement that fuel/air ratio control is indispensable.

Furnace Pressure—Automatic furnace pressure control or, alternatively, working to preset values by hand control, is now accepted as an integral part of any open-hearth furnace operating procedure. The impulse from the roof-pressure measurement is used to initiate movement in the stack damper and in the butterfly damper on the boiler, or, in one or two cases, to vary the speed of the boiler fan. Where fuel/air ratio control based on air measured at the valve gear is applied, furnace-pressure control is vital, because it ensures that the air infiltration likely to occur at low measured air flows will be at a minimum.

Furnace Reversal—The need for automatic reversal is twofold: firstly, to simplify the work of the furnaceman, giving him more time to attend to other items of furnace operation; and, secondly, to achieve and maintain the maximum efficiency of the regenerative cycle. The optimum reversal-time cycle for a particular furnace can be ascertained approximately from the characteristic checker and flue temperature/time curves. The general trend resulting from such studies has been to reduce the cycle from the old-fashioned ½-hr. reversal to times ranging between 10 and 15 min., and, in one or two exceptional cases, between 6 and 12 min. The time taken to reverse can vary from about 15 sec. on a good installation to as much as 50 sec. on other types, so that the more frequent the reversal, the more important becomes the speed of reversal. On a 10-min. reversal cycle, and taking 20 sec. to reverse, the fuel will be out of the furnace for 3·3% of the time, so that a slow-reversing equipment can easily cancel out any advantage to be gained from reducing the time of the reversing cycle. The modern American equipment for reversal control is exceedingly elaborate and

flexible, and reversals can be initiated in a variety of ways, as follows:

(*a*) By timing units operating one on each end, giving the ability to vary reversal times on unbalanced furnaces

(*b*) By timing units as in (*a*), supplemented by a temperature controller working on the temperature difference between the two ends. In this case, the temperature controller initiates reversals, except when the time required falls outside the maximum or minimum limits set on the timing units, in which case the latter initiates reversals

(*c*) As in (*b*), but with a further modification enabling reversals to be initiated by a high checker temperature in addition to the initiation provided under (*a*) and (*b*).

This equipment also includes arrangements to lock steam valves in a fixed position during reversal. In British liquid-fuel-fired furnaces, using roof-temperature fuel flow linkages, the necessity to lock the fuel valve during reversal is particularly important. Without this, there is usually a rapid increase in fuel flow to meet the slight roof-temperature drop during reversals, and as the flow of steam and air is slightly out of balance at this time, there is a rapid creation of thick black smoke in the furnace. This, in turn, further depresses the roof temperature and initiates an impulse for even more oil, which aggravates the difficulty.

CONCLUSIONS

Some of the most important factors affecting the productivity and fuel consumptions in open-hearth furnaces are the types of fuel used, the constitution of the charge, furnace size and design, furnace availability, and furnace operation and control.

The liquid fuels, oil, tar, and creosote pitch, together with natural gas, give production rates 5–10% higher than the leaner fuels such as mixed gases or producer gas, with reductions in fuel consumptions of the same order.

For the Park Gate furnaces, the effect of variations in the metallurgical load, resulting from changes in charge constitution, has been assessed in terms of slag bulk related to output/hr. and of fuel consumption/ton related to output/hr. Both these relationships are highly significant when examined on a statistical basis. In the furnaces examined, there is an optimum charge constitution in the region of 35% of hot metal and 65% of scrap. For the better-quality American pig iron, there is no optimum charge constitution and there is a progressive increase in productivity with increasing hot-metal content over the range 20–55% of hot-metal content.

Increase in furnace size, as measured by tapping capacity, has a very important beneficial effect on both productivity and fuel consumption.

In furnace design, better operating results can be obtained by using shallower baths; *i.e.*, increasing the hearth area per ton of tapping capacity by using single uptakes, especially on small furnaces, and by using pigeon-hole-type checkers instead of the solid-flue type.

Improvements in plant layout, which permit the extended use of mechanical equipment for repairing furnaces, can give increases in shop productivity by increasing furnace availability.

Furnace operation and control offer three means of improving productivity and lowering fuel consumption: combustion-control technique, slag-control practice, and the determination of bath temperatures. Although these three subdivisions are usually dealt with as separate items, they are very closely interrelated, and the combined effect of all three is important, as it also includes the added advantages of improvements in steel quality. The basis for any combustion-control system should be to give the fastest possible charging conditions coupled with maximum furnace temperatures during melting and refining. High fuel flows in the charging period, giving fast charging rates, are associated with maximum productivity and minimum fuel consumptions in a given furnace.

Acknowledgments

The author would like to thank the Directors of the Park Gate Iron and Steel Co., Ltd., for permission to publish the paper and for the advice and encouragement received in its preparation.

Thanks are also due to many colleagues at Park Gate, and to members of the British Iron and Steel Productivity team for their co-operation.

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Heat Recovery in Industrial Furnaces

By A. Clift and C. Knight

SYNOPSIS

Important features of both direct and other forms of heat recovery in industrial furnaces are described, and possible future developments are indicated.

Introduction

THE subject ' Heat Recovery in Industrial Furnaces ' has been considered in various ways in recent times, and it appears to be far too large a subject for general development in a single paper. No long theoretical discussion has therefore been attempted, and the present paper is confined to a description of what appear to the authors to be the important features of heat recovery, gained from their experience with a variety of types of industrial furnace installations.

Heat recovery of some form has probably been practised ever since reheating furnaces were used. In early days in Britain, however, this often took the form of a refractory recuperator installed in an inaccessible position, so that the unit became inoperative when the brick elements began to decay, usually after only a short period of service.

The typical unit shown in Fig. 1 was put down to reheat tubes for hot reducing, the furnace being producer-gas fired, and the combustion air preheated in a refractory recuperator built-in under the furnace hearth. After a relatively short period of operation, the recuperator began to break down, the combustion air mingling with the exhaust gases, and finally there was almost complete loss of control. To keep the furnace in operation, it had to be converted to oil firing, and air recuperation was practised in an improvised metallic unit, placed in the flue at a distance from the furnace structure.

Heat recovery is practised with the general intention of achieving higher overall efficiency. The principal need on a particular installation may not always be clear-cut, but it can generally be said that the builders and users of furnace equipment are aiming to achieve:

(1) Economy in fuel
(2) A satisfactory operating temperature, sometimes using fuel that would not be possible with cold air
(3) Greater speed and reliability of the operations in hand.

Some furnace equipment may inevitably have to be installed in situations where heat recovery is not possible, and where furnace efficiency is of minor importance compared with overall plant efficiency. Such circumstances are rare and are usually associated

Manuscript received 16th June, 1952.
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Table I

PERFORMANCE OF CONTINUOUS-WELD FURNACE RECUPERATORS

Performance		Recuperator No. 1	2	3	4	5	6
Recuperator pressures, in. W.G.	Top 1	+0·01	+0·03	+0·07	+0·12	+0·12	+0·16
	2	−0·02	+0·02	+0·04	+0·04	+0·03	+0·08
	Middle 3	−0·04	−0·04	−0·02	−0·01	−0·01	+0·02
	4	−0·10	−0·12	−0·03	−0·03	−0·10	−0·01
	Bottom 5	−0·18	−0·20	−0·14	−0·10	−0·16	−0·02
Cold air pressure entering recuperator, in. W.G.		9·5	10·4	10·8	10·8	12·4	12·4
Hot air pressure leaving recuperator, in. W.G.		6·0	8·3	6·8	6·5	7·7	7·9
Pressure in recuperator uptake, in. W.G.		+0·02	+0·04	+0·06	+0·10	+0·14	+0·12
Pressure in furnace uptake, in. W.G.		−0·02	+0·03	+0·03	+0·04	+0·04	+0·04
Hot air temperature, ° C.		540	600	680	550	600	580
Waste gas temperature, ° C.		360	400	450	370	400	330
Recuperator top layer element temperature, ° C.		820	900	1030	880	910	940
Furnace temperature before recuperator entrance		960	1280	1260	1300	1430	1450
Air flow, cu. ft./hr., × 10^3		160	170	160	165	175	185
Gas flow, cu. ft./hr., × 10^3		22	25	29·5	23	22	26·5

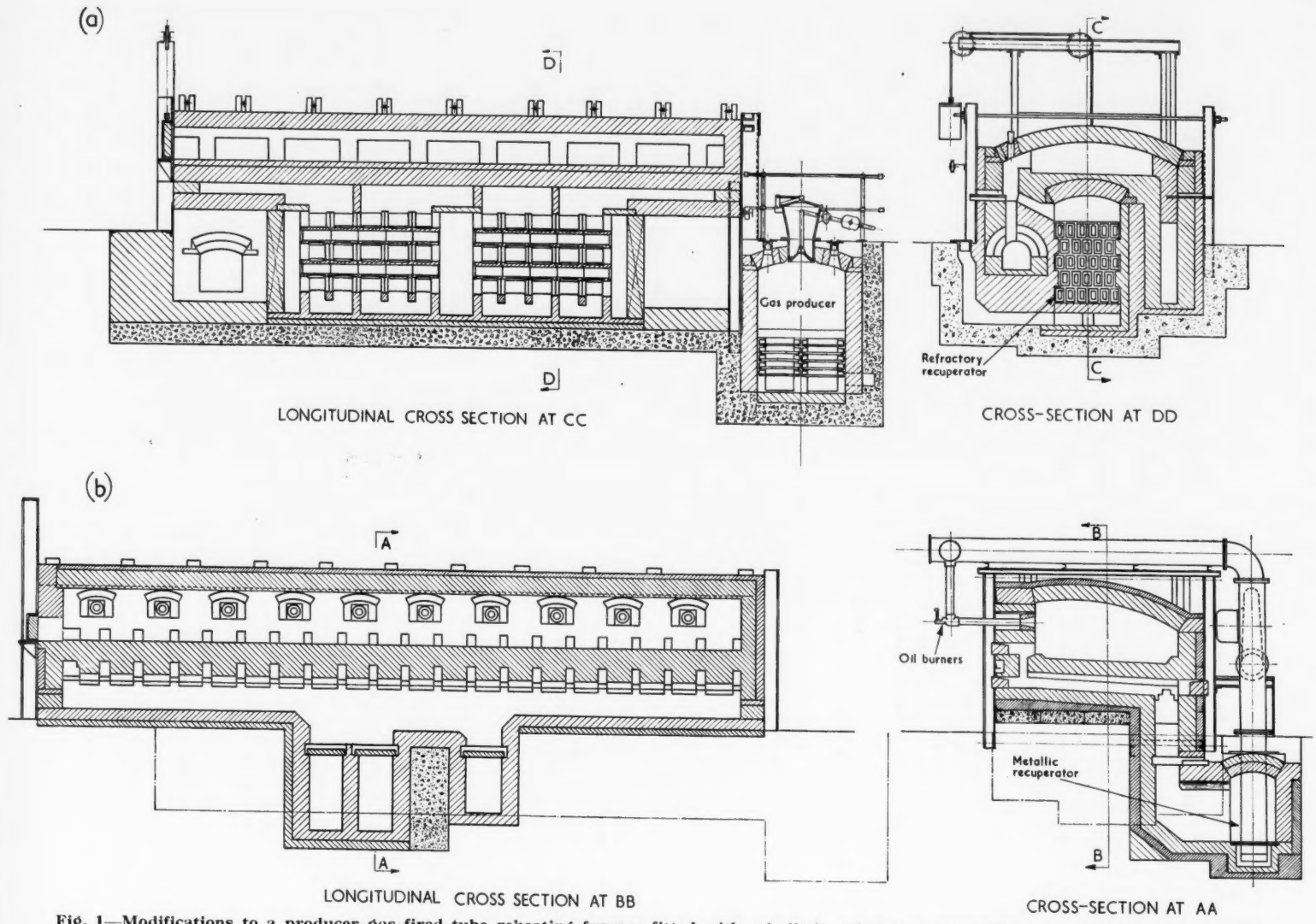


Fig. 1—Modifications to a producer-gas-fired tube-reheating furnace fitted with a built-in refractory recuperator: (*a*) Original arrangement; (*b*) modified arrangement

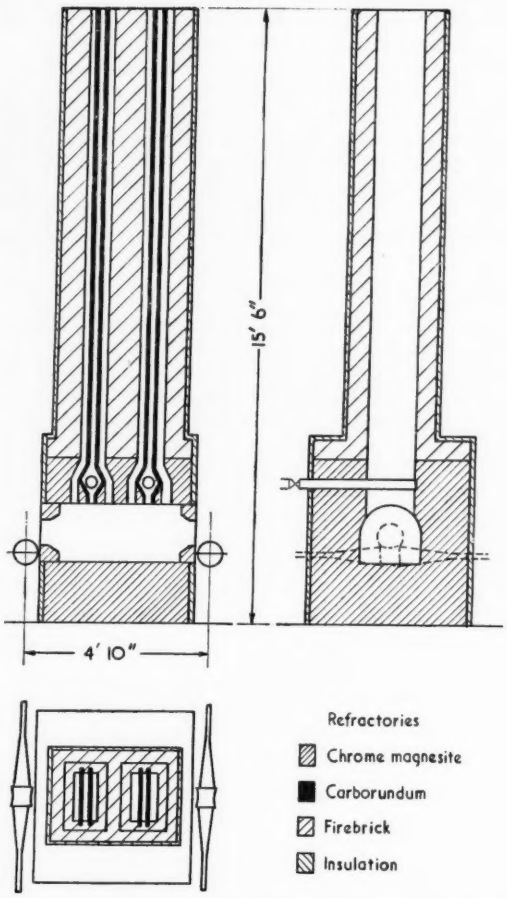


Fig. 2—Mixed-gas-fired high-intensity continuous tube-reheating furnace, with tower-type refractory air recuperator

(a) Original arrangement; (b) modified arrangement

with difficult layout conditions, when the furnace may be located in the centre of a mass of heavy engineering equipment and flues are impossible.

The economy aspect has been very thoroughly investigated in the past few years, mainly because of the national fuel position, but also to achieve smoke abatement and general efficiency of the plant, with minimum expenditure.

DIRECT HEAT RECOVERY

Temperature Requirements

There must be many furnaces, both in Britain and abroad, where some form of direct heat recovery is used to obtain particularly high working temperatures. The type of unit that has aroused most interest in recent times, however, is the 'hell-hole' furnace. This aims at achieving heating conditions as near as possible to those obtained with electric induction heating.

The source of temperature rise in the case of induction heating is, of course, within the stock; and, owing to the extremely high rates of energy input possible, very fast speeds of heating can be obtained. Where gas or oil firing has to be employed, it is a necessary criterion that both the flame temperature and the temperature of the furnace refractories must be very high. The American approach to this condition has been on the basis of using pre-mixed natural gas and cold air, admitted through an extremely fine slot refractory burner, so that combustion is instantaneous and almost flameless. Very high working temperatures are possible in this way.

Natural gas is not available in Britain, and, in fact, the gases readily available are often well below 500 B.Th.U./cu. ft. In an attempt to secure the same working temperatures as those in the American units, but employing a gas having a calorific value of only 300 B.Th.U./cu. ft., a unit was developed which used a refractory recuperator for preheating the combustion air (Fig. 2). Its purpose was to reheat

Table II

COMPARISON OF REGENERATOR SIZES

Details	A*	B	C
Type of furnace	Tilting	Fixed	Fixed
Rating	60 tons	60 tons	100 tons
Fuel	Producer gas	Producer gas	Tar/coke-oven gas
Air Regenerator			
Length	17 ft. 9 in.	18 ft. 0 in.	20 ft. 1½ in.
Height	12 ft. 3 in.	13 ft. 0 in.	16 ft. 10½ in.
Width	8 ft. 10½ in.	13 ft. 0 in.	17 ft. 10½ in.
Volume	1930 cu. ft.	3040 cu. ft.	6120 cu. ft.
Openings	6½ in. × 6½ in.	6½ in. × 6½ in.	6½ in. × 6½ in.
Gas Regenerator			
Length	17 ft. 9 in.	18 ft. 0 in.	
Height	12 ft. 3 in.	13 ft. 0 in.	
Width	6 ft. 7½ in.	7 ft. 9 in.	
Volume	1447 cu. ft.	1815 cu. ft.	
Openings	6½ in. × 6½ in.	6½ in. × 6½ in.	
Total volume	6754 cu. ft.	9710 cu. ft.	12,240 cu. ft.
Volume per ton of steel	112·5 cu. ft.	162·0 cu. ft.	122·4 cu. ft.

*This unit has now been converted to fuel-oil firing, but, apart from employing the gas chambers as additional air regenerators, no alteration has been made to dimensions

tube or bar in a continuous flow line for further processing and several of these units were to be installed in the spaces between the rolls of a conveyor track. Temperatures up to 1800° C. in the heating chamber were made possible by air preheats of about 1000° C. As the stock needed to be brought up to only 1000° C., the heating rate was extremely fast, and furnace scaling was negligible. Such an installation needs little more space than that required by the roller conveyor carrying the stock from the rolling mill to the next process.

Furnace units embodying simple refractory recuperators, located over the furnace heating chamber, are in use in Britain, but, so far as is known, this is the first time that an attempt has been made to develop this type of structure for the class of work described.

Most furnaces working on lean fuels, where high operating temperatures and rapid concomitant heating rates are required, must have some form of direct heat return, sometimes to both gas and air. Examples are to be found in soaking pits; instances of the different designs are illustrated in Figs. 3, 4, and 5. The fuel employed on these soaking-pit installations is mixed clean gas which, without preheat, would be expected to develop temperatures of about 1000°–1100° C. only; with preheat of air and gas, temperatures of 1350°–1400° C. may comfortably be reached.

Speed of Operation

A different kind of unit working at high heating rates is the continuous-strip heating furnace used for making welded tube. Four units of fundamentally similar design are installed at the tubeworks at Corby, the largest being 180 ft. long, two of the others 160 ft.

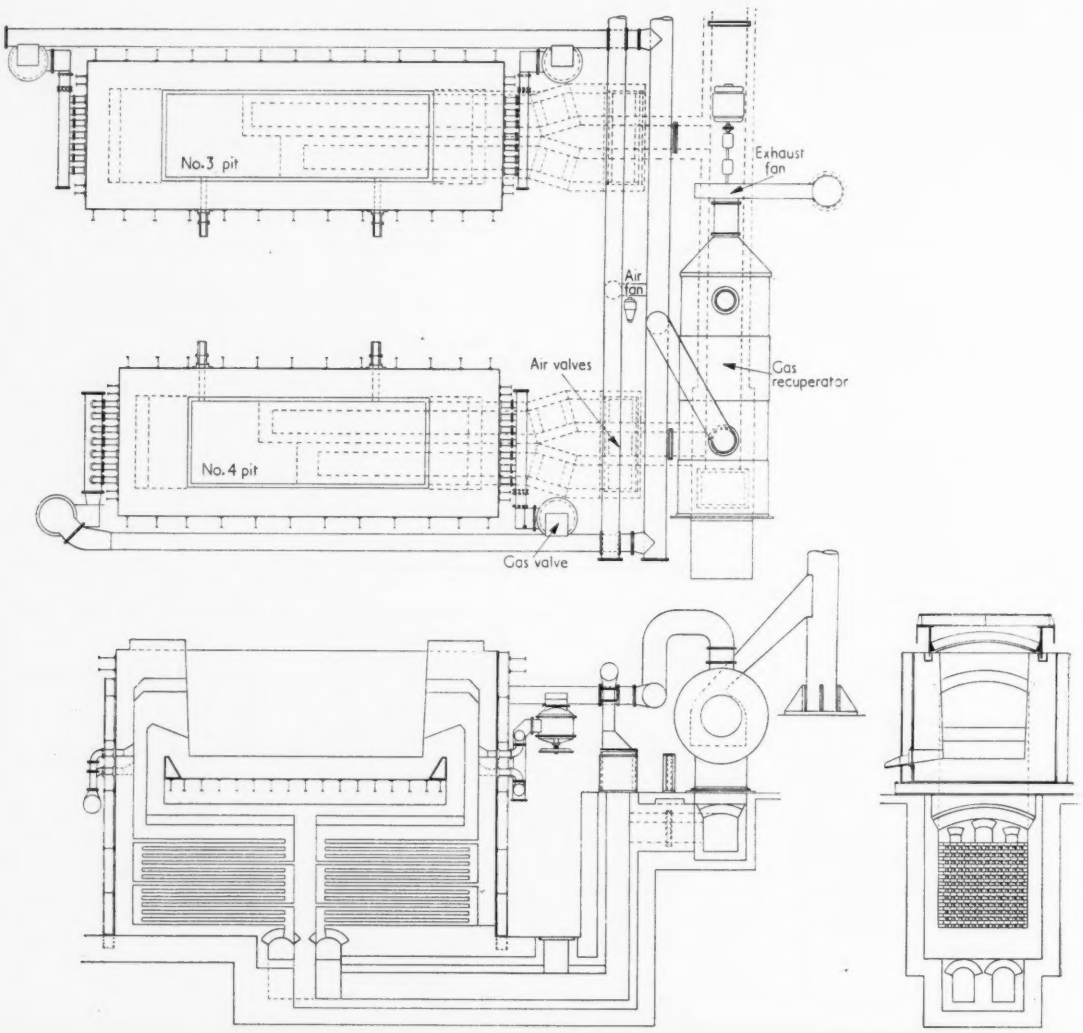


Fig. 3—Clean-gas-fired soaking pits, with air regenerators and metallic gas recuperators

long, and the fourth 120 ft. long. The output of the large furnace is about 40 tons of strip per hour, raised from cold to welding temperature, the heating rate being arithmetically equivalent to about 4 min. per inch of strip thickness.

The fuel is coke-oven gas with a calorific value of 500 B.Th.U./cu. ft., and the high working speeds are made possible by preheating the combustion air in metallic needle-type recuperators. Six of these recuperator units are installed at suitable positions along the furnace, which is divided into appropriate sections for controlling gas and air input. The early design was based on a gradation in size of the recuperators, from the hot to the cold end of the furnace, to suit the changes occurring with furnace-gas temperatures and air requirements. However, as an unwieldy number of spare components would have to be carried, it was decided to build the units to a common size and arrangement, even though some slight loss of efficiency might be incurred. In fact, the units are giving an extremely satisfactory performance.

On the air side of the recuperator units shown in Fig. 6, the elements are installed three-pass, waste gas being single-pass. The hot gases leaving the furnace chamber are diluted with cold air to reduce the maximum temperature to 1050° C., so as to protect the metallic elements. The top bank of the metallic elements is in 35% chromium iron, the centre bank in normal heat-resisting iron, and the bottom bank in plain cast iron. Some typical performance data for the six units are given in Table I.

Another instance of relatively rapid heating rates is found in a mill producing large steel pipes from round ingots. Originally, the ingots, up to 17½ in. dia., were heated in conventional roll-down furnaces. The practice—handed down over the past years—was to allow at least 60 min. on the hearth for each inch of diameter, the idea being that the longer the stock was allowed to soak, the better was the quality of heating.

With the introduction of improved mill equipment, however, the old furnaces became inadequate, and it was decided to replace them with a new car-type continuous unit arranged for raw producer-gas firing and fitted with a metallic air recuperator. With a furnace of this type, the stock is, of course, stationary on the cars, and slagging must be avoided. Preliminary investigations proved that, despite these limitations to rapid heating, the new furnace could be designed on the basis of a heating rate of 40 min. per inch of diameter.

With the new furnace, illustrated in Fig. 12, it was

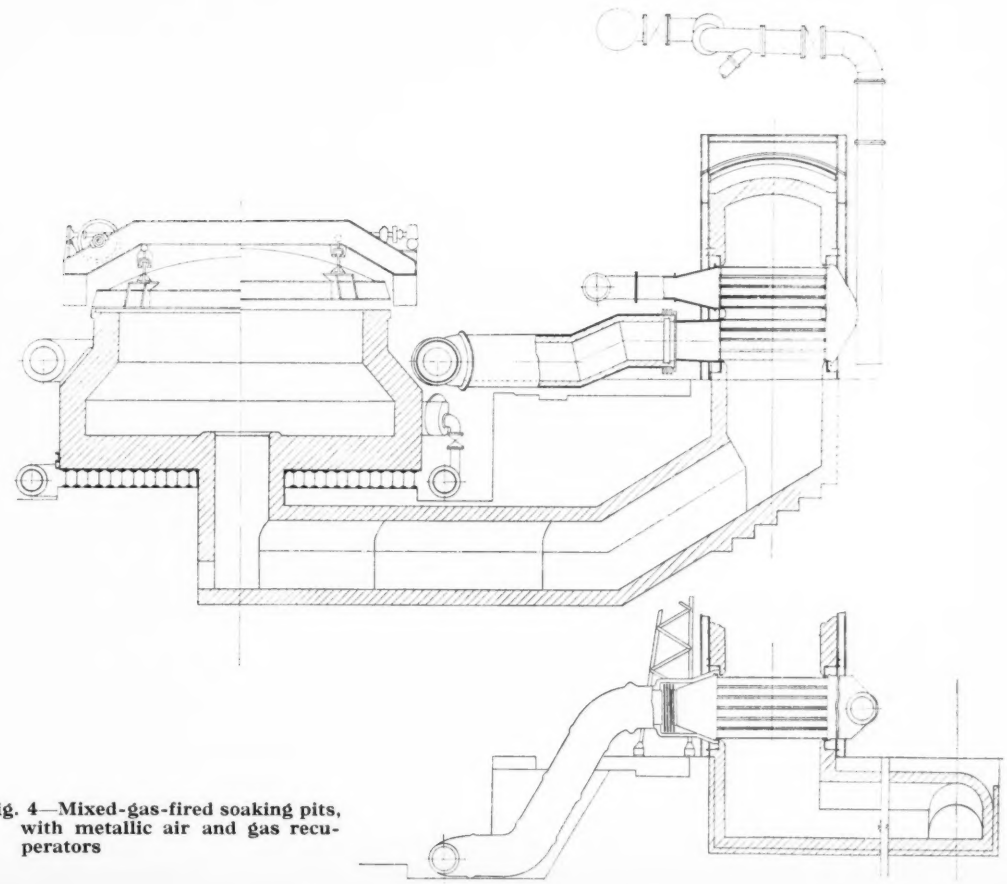


Fig. 4—Mixed-gas-fired soaking pits, with metallic air and gas recuperators

found that, with both stock and air recuperation, this heating rate could easily be achieved without detriment to the quality of heating. Subsequently, with the advent of low-alloy nickel-bearing steel, scaling became a problem and, to minimize this, heating was speeded up by charging ingots in single layers. At present, the furnace is operating at heating rates of 20–30 min. per inch of ingot diameter, depending on ingot size, with an all-round improvement in process efficiency, quality, and sensitivity of furnace control.

Recuperators

The general difficulty of obtaining satisfactory refractory material for the manufacture of heat-recovery equipment has thrown considerable emphasis on metal elements. In the authors' company, metal-type recuperators are used in most of the reheating equipment. They lend themselves to good design and easy and rapid maintenance, and are also highly efficient.

The principal troubles have been associated with corrosive or dirty waste gases, but even these become less acute if the recuperator is correctly designed. The attention of the designers of heat-recovery equipment of this type should be drawn to the advantages that might accrue if the recuperator could be so constructed that it could be built on a unit basis. This was shown during a replacement of one of the 165-ft. long continuous-strip heating furnaces already mentioned. To minimize loss of production, the components of a new and larger furnace had been pre-

Table III

PERFORMANCE OF RECIRCULATING GALVANIZING FURNACE

Average Output	**Tubes**	**10–11 tons/hr.**	
	Spelter	**0·9 tons/hr.**	
Fuel Consumption (based on coke-oven gas)	**12,000–13,000 cu. ft./hr.**		
Heat Balance	**Temp., °C.**	**B.Th.U. ×10⁶**	**%**
Heat input per hour			
(1) From combustion of fuel	...	**4·96**	**100·00**
Distribution per hour			
(2) Heat to steel tubes	**50–450**	**2·21**	**44·50**
(3) Heat to spelter	**50–450**	**0·28**	**5·65**
(4) Discharged gases	**815**	**2·18**	**44·00**
(5) Radiation losses	...	**0·29**	**5·85**
		4·96	**100·00**
Thermal efficiency			**50·15**

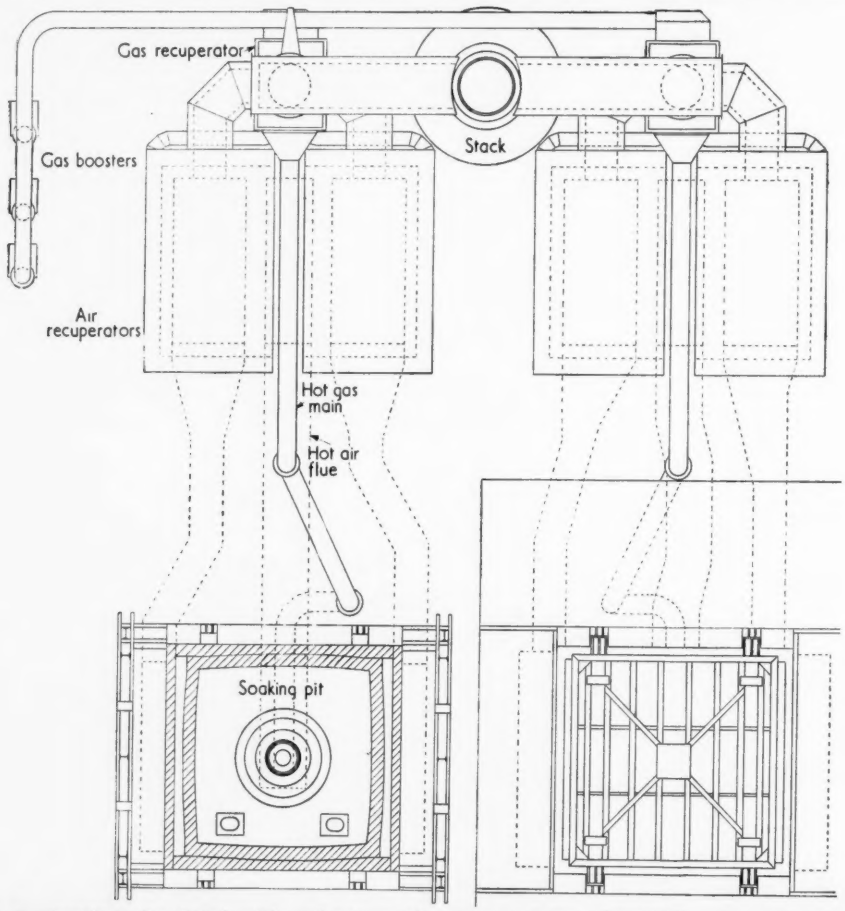


Fig. 5—Soaking pits fitted with refractory air recuperators and metallic gas recuperators

viously assembled at a standby site. At a suitable opportunity the old furnace was demolished, and the new furnace was transferred to the site with a loss of only 10 days' production time. This, so far as is known, is a record for this kind of rebuild. The old recuperators were unsuitable for the new design, and they were therefore lifted out by crane without taking precautions to avoid damage. Even so, the damage was so slight that it seemed probable that they could have been removed unharmed if the design had been appropriately modified.

To minimize the effect of dust carry-over, the new recuperators were considerably larger in size than the previous units, as illustrated in Fig. 7. The progress of the rebuild is also illustrated in Fig. 8.

Another feature of interest in this rebuild was the abandonment of stock preheating. When the old unit was first installed, about one-third of the hearth at the ingoing end was unfired, but by drawing a proportion of the furnace gases down to this end, some stock recuperation could be secured, with an improvement in furnace efficiency, in terms of the mill demands at that time. As the mill demands increased, it was necessary to burn more fuel, and additional burners were installed in the part of the furnace previously unfired. A new fourth recuperator was also added. In the new furnace rebuild, where mill demands were again increased, this stock recuperation had to be abandoned altogether, as still more fuel had to be burnt and the available site did not permit an increase in the furnace length. The performance of the furnace indicates that the measure of thermal efficiency lost by abandonment of stock recuperation has been recovered by the extra capacity provided for air recuperation.

Regenerators

It had been the authors' considered opinion, up to the end of the war, that regenerators in Britain were generally too small. When an opportunity arose to build a new open-hearth installation, the regenerators were designed to a size proportionately larger than any other similar units in Britain. Their performance was satisfactory, but it was suspected that they were unnecessarily large, so that for the second open-hearth furnace plant the regenerator

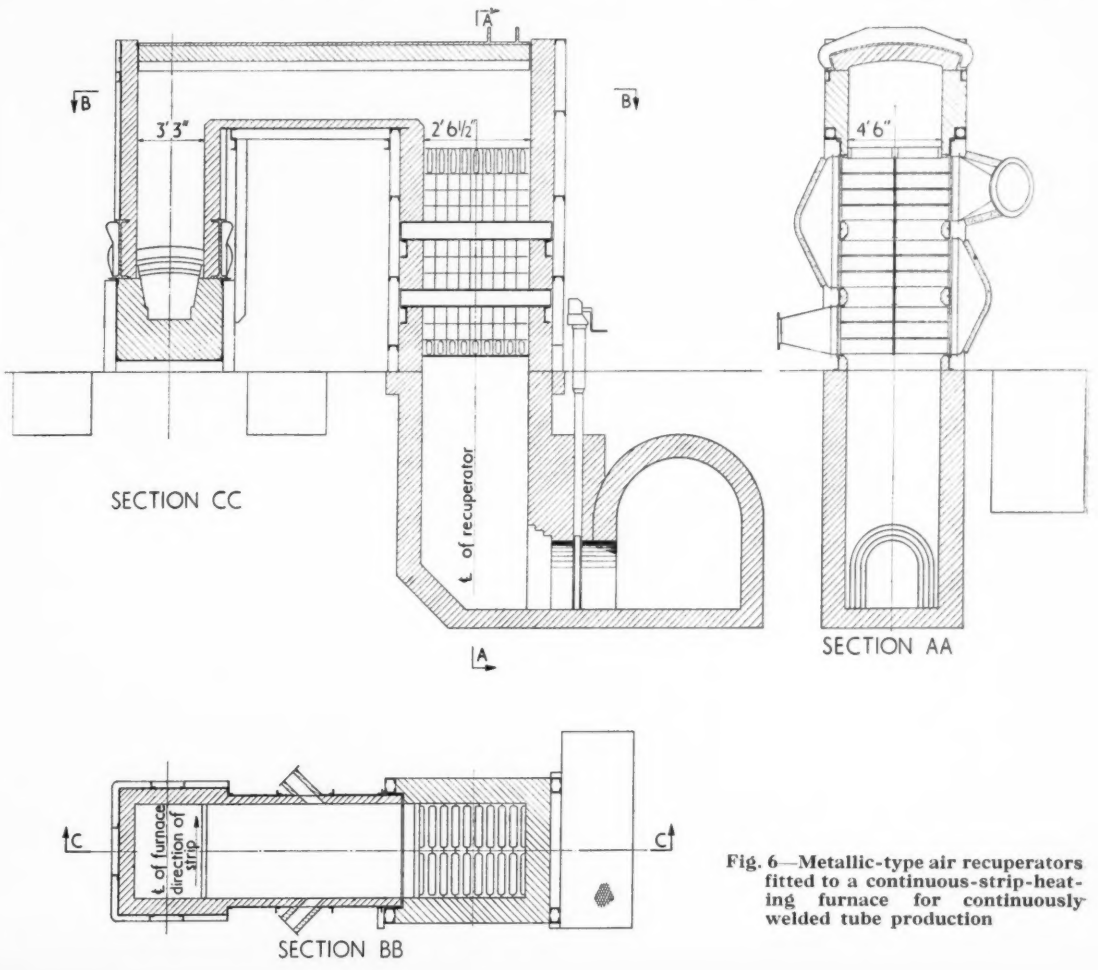


Fig. 6—Metallic-type air recuperators fitted to a continuous-strip-heating furnace for continuously-welded tube production

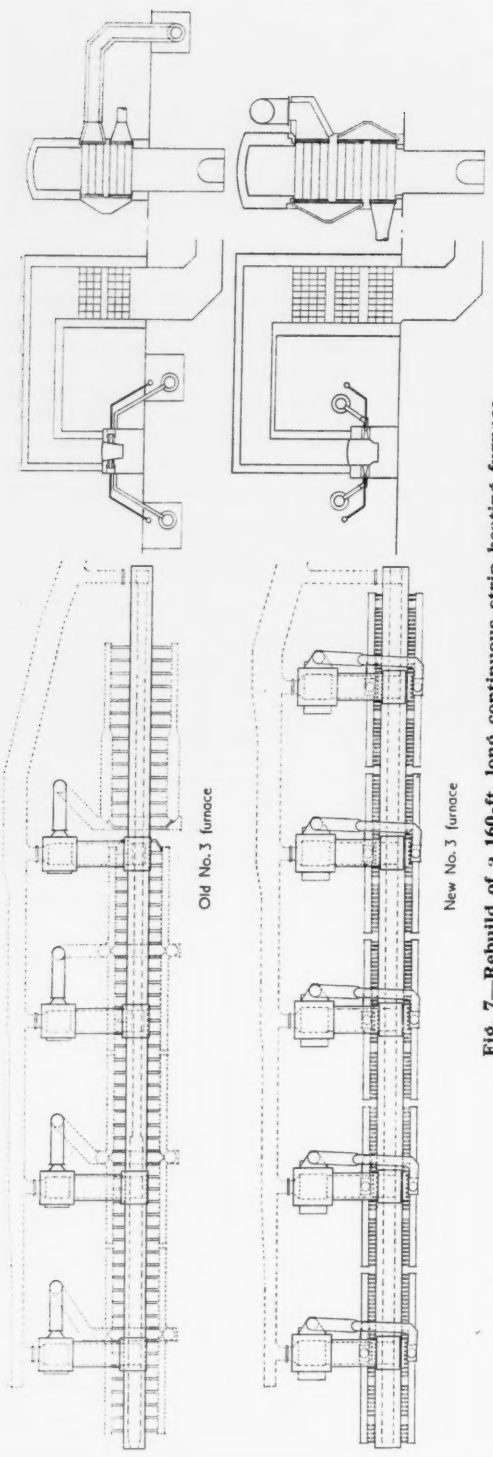


Fig. 7—Rebuild of a 160-ft. long continuous-strip-heating furnace

size was reduced. Performance was again satisfactory, proving that it is indeed possible to build regenerators on too large a scale. The sizes of the regenerators are given in Table II, compared with a typically small unit in use for many years. It is apparent that there is still much to be learned about the general design and size of regenerators.

OTHER FORMS OF HEAT RECOVERY

The recovery of heat and its return to the furnace reflect directly on the furnace efficiency, and this is naturally one of the first concerns of the furnace personnel. Figure 9 indicates some of the possible savings.

At the same time, installations may not always be economical where recuperation is the only method of heat recovery, particularly where furnace-gas temperatures are low. It is generally considered to be uneconomic to instal recuperators where the temperature of the gases leaving the furnace is less than 450° C. As an example, a recuperator was provided in a bogie batch-type furnace, installed for the heat-treatment of special steam-mains bends, to obtain a theoretical saving of about 10% of the fuel. In fact, owing to intermittent operation of the unit, the exhaust flue system and recuperator never had the opportunity to build up to good temperature conditions, and thus the fuel saving was virtually nil. In such cases, many and varied forms of heat recovery may have to be practised to secure the maximum possible returns. The following examples will possibly be of interest.

Recirculation of Waste Gases

The combination of high fuel efficiency with safety in operation, coupled also with high outputs, has always been difficult to attain with galvanizing pots. Direct firing has generally proved a somewhat dangerous practice, and where direct flames are employed it is most common to use a bridge wall, which, in effect, also tends to act as a heat storage. It is thought that Stewarts and Lloyds was the first company in Britain to employ a modern development of recirculation of the products of combustion for this kind of duty; they have several units of this type in operation in the Midlands. The fuel, either oil or gas, is fired in a separate furnace; the hot products of combustion are led to the galvanizing pot, and thence returned *via* an induced draught fan to the furnace, as shown in Fig. 10. Some data on performance are given in Table III.

The particular need in a unit of this sort, working at high rates of production, is to ensure safety in operation and a reasonably long life for the pot. It is doubtful whether a recuperator could effectively be used, but in most galvanizing plants there are other outlets for employing waste heat. These are pickle and flux bath heating, stock drying and preheating, and the like. It is considered that better use of the waste heat discharged from the galvanizing furnaces can be made in this way, and any future development will probably be along these lines.

Heat Storage in Furnace Refractories

Figure 11 shows the arrangement of a furnace for under-hearth firing. The extra mass of brickwork,

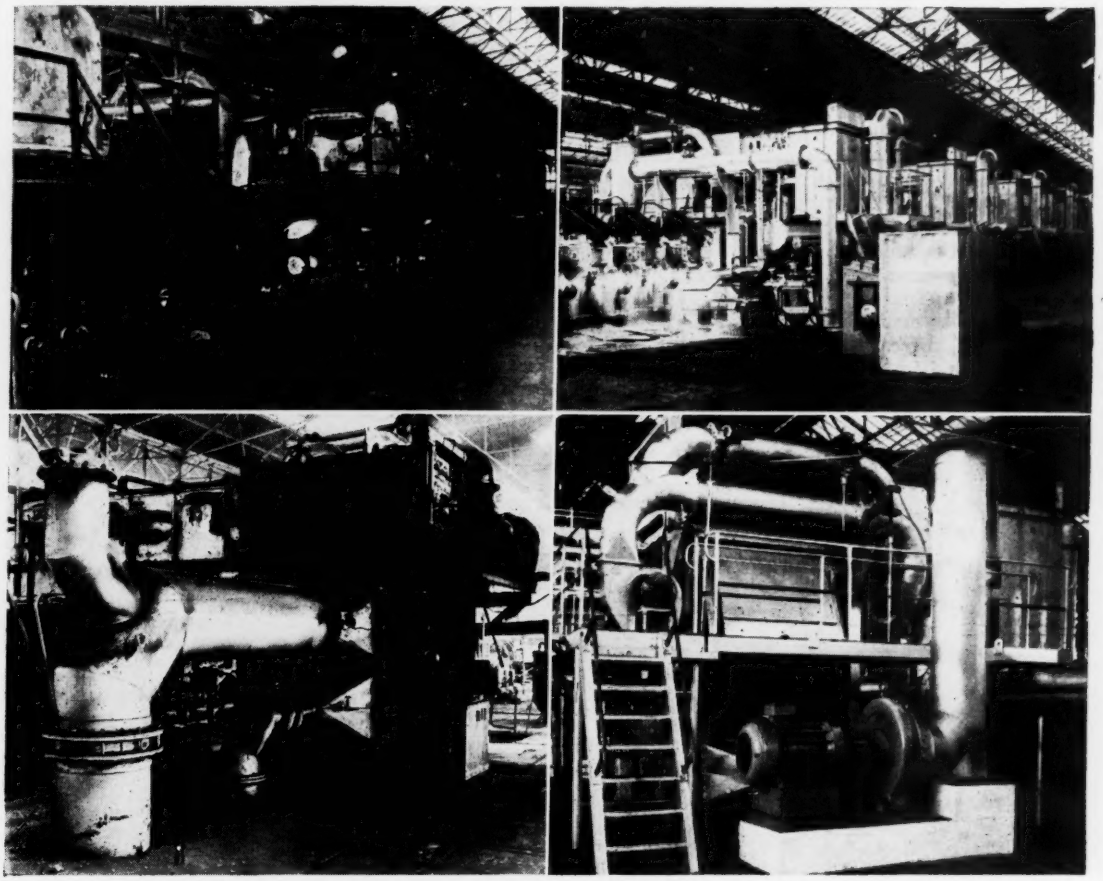


Fig. 8—Progress of the rebuild of C.W. furnace: (*a*) View of old continuous-strip-heating furnace in operation; (*b*) 2-pass recuperator attached to old continuous-strip-heating furnace; (*c*) new continuous-strip-heating furnace completed and gassed-in after 10 days' loss of production; (*d*) 3-pass recuperator attached to new continuous-strip-heating furnace (*cf.* Fig. 8*b*)

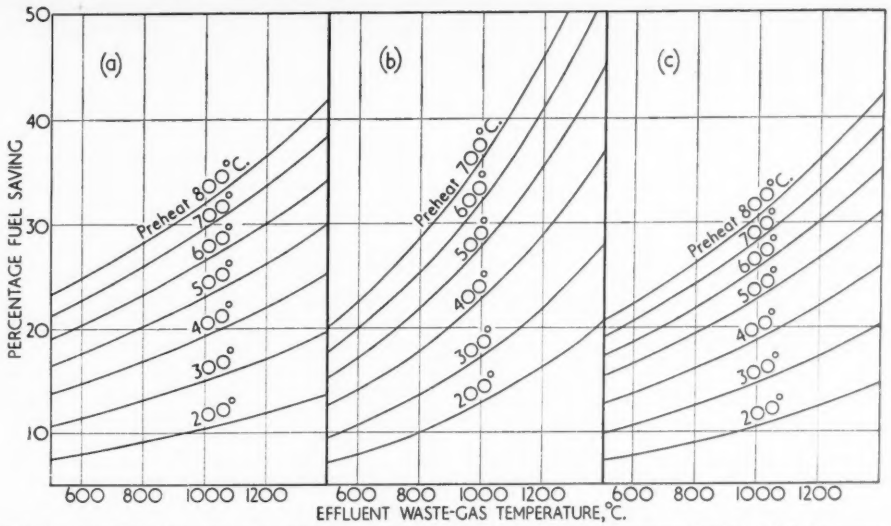


Fig. 9—Possible fuel savings with air recuperation for common gaseous fuels: (*a*) Coke-oven gas; (*b*) blast-furnace gas; (*c*) producer gas

which is heated from both sides, constitutes a fair heat storage, and is of particular advantage where low-temperature working conditions are necessary.

Combination of Recuperative and External Heat-Recovery Equipment

Stock recuperation, followed first by usual air or gas recuperation, and then, if possible, by other heat-recovery installations, such as waste-heat boilers, constitutes an excellent method of scavenging as much as is practicable of the heat carried away by the gases from the furnace. The continuous car-type ingot-reheating furnace, mentioned earlier and illustrated in Fig. 12, is an arrangement of this type. The furnace is about 159 ft. long, and about one half of this is fired, the balance of the unit being used for stock preheating. A metallic needle-type recuperator is provided for air preheating and is followed by a waste-heat boiler for steam raising. With this combination of heat recovery, it is possible to reheat the ingots with an average fuel consumption as low as 1·5 cwt. of coal per ton of steel for one week's production, with a concomitant raising of steam of over 5000 lb./hr. at 80 lb./sq. in. pressure. The overall thermal efficiency of the installation is 59·2%, which is very good for this class of work.

More detailed information, in the form of a heat balance, shown in Table IV, has been provided in connection with a similar car-type furnace employed for reheating steel slabs to 1310° C. but fired with mixed gas and provided with air regenerators instead of recuperators. There is no waste-heat boiler in this installation, but, from the evidence recorded, it would have been possible to raise something like 4000 lb. of steam per hour if a unit had been available. This

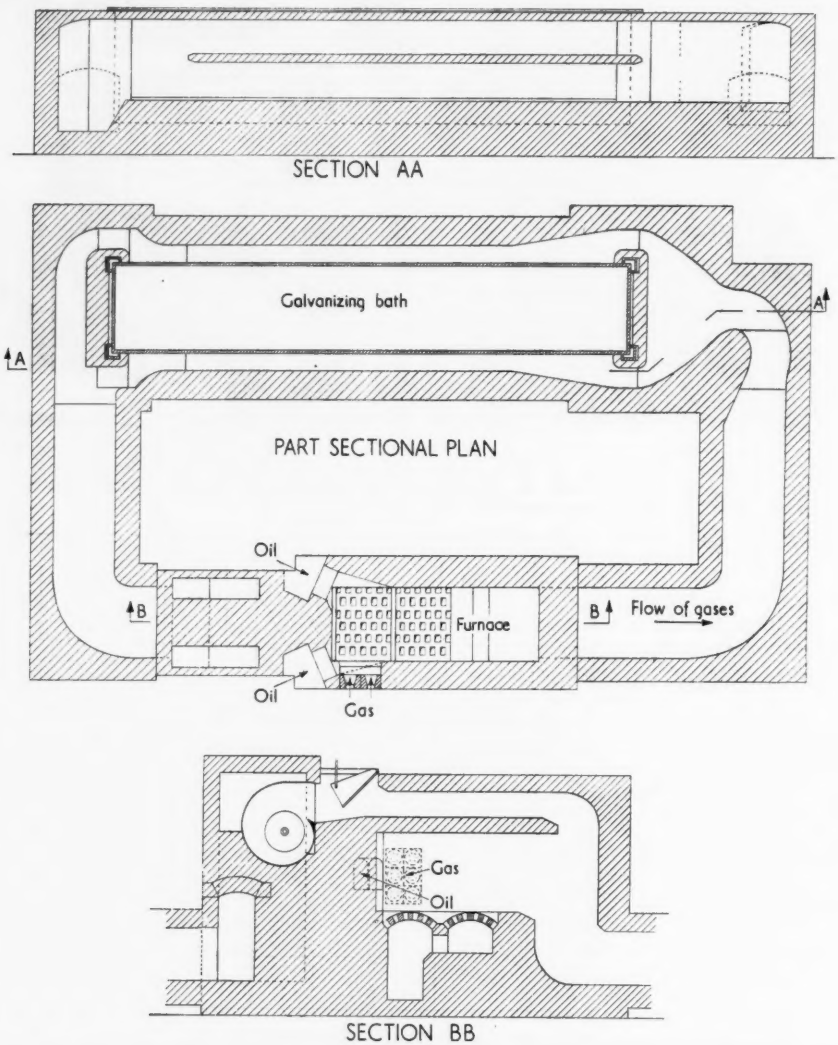


Fig. 10—Oil- or gas-fired galvanizing furnace recirculating hot furnace gases

Table IV

PERFORMANCE DATA AT A CONTINUOUS SLAB REHEATING FURNACE, MIXED-GAS FIRED AND PROVIDED WITH AIR REGENERATORS

Details

Stock dimensions	40·5 in. × 8·3 in. × 75·0 in. × 7160 lb.
Throughput	12·46 tons/hr.
Charging temperature	50° C.
Temperature leaving preheating zone	1175° C.
Discharge temperature	1310° C.
Fuel consumption (203 B.Th.U./cu. ft.)	10,060 cu. ft./ton of steel

Heat Balance

	B.Th.U./hr.	B.Th.U./hr.	%
To Heat Supplied:			
By sensible heat in gas	−30,000		
By combustion of gas	25,460,000		
By heat of formation of scale (1¾% charged weight)	1,360,000		
		26,790,000	
By Heat Utilized:			
Heat given to steel		10,220,000	38·2
Heat rejected to chimney			
By dry waste gas	6,892,000		
By moisture in gas used	53,000		
By moisture from combustion	633,000		
By moisture in air used	86,000		
		7,664,000	28·6
		17,884,000	
Heat lost while drawing slabs, in empty bogies, radiation, etc.		8,906,000	33·2

Supplementary Heat Balance ***(showing approx. conditions in each half of the furnace***

	Preheating Zone			Soaking Zone		
	B.Th.U./hr.	B.Th.U./hr.	%	B.Th.U./hr.	B.Th.U./hr.	%
To Heat Supplied:						
Sensible heat in gas	−19,000			−11,000		
Combustion of gas	16,240,000			9,220,000		
Heat of formation of scale	450,000			910,000		
		16,671,000			10,119,000	
By Heat Utilized:						
Heat given to steel		9,110,000	54·5		1,110,000	11·0
Heat rejected to chimney						
By dry waste gas	4,432,000			2,460,000		
By moisture in rich gas	26,000			27,000		
By moisture from combustion	313,000			320,000		
By moisture in air used	59,000			27,000		
		4,830,000	29·0		2,834,000	28·0
		13,940,000			3,944,000	
Heat lost by radiation, door openings, etc.		2,731,000	16·5		6,175,000	61·0
		16,671,000			10,119,000	
Mean temperature of slabs leaving zone		1175° C.			1310° C.	
Thermal efficiency			54·5			11·0

would have increased the overall efficiency of the plant from 38·2 to 55·5%.

Steam Raising *versus* Recuperation

Where conditions do not permit the employment of both recuperation and steam raising, the choice between the two is often the subject of controversy. In one instance, it was found advisable to eliminate recuperators altogether and to employ waste-heat boilers as the sole method of heat recovery. The unit in question was a billet-reheating furnace designed initially for firing with mixed clean gas, but also with provision for liquid fuel. Changes in the fuel position at the works made liquid-fuel firing a necessity and, bearing in mind that the required working temperatures could be secured without air preheat, coupled with the fact that site conditions made a recuperator installation difficult, the proposal to employ heat recovery of this type was abandoned in favour of a boiler installation. This made it possible to employ a much lighter-duty exhausting fan. Some of the possible alternatives considered in the above connection are given in Table V, and it will be seen that, on balance, the greatest fuel saving—as reflected in the column headed "Net Fuel Consumption"—was secured by the elimination of recuperators and the use of ample-size waste-heat boilers.

Another example where the recuperators also had to be abandoned was found at a plant for the manufacture of small forgings and stampings. A number of regenerative furnaces fired with producer gas were used, but for various reasons it was necessary to convert to oil firing. A prototype unit was built, at first without air recuperation, although this was to be added later, and it proved to be a very fast and reliable heater—too fast, in fact, to permit the employment of preheated air. The conversion of the remaining gas-fired units has therefore proceeded on the basis of firing with cold air. Heat recovery will have to take some other form, probably the employment of a waste-heat boiler.

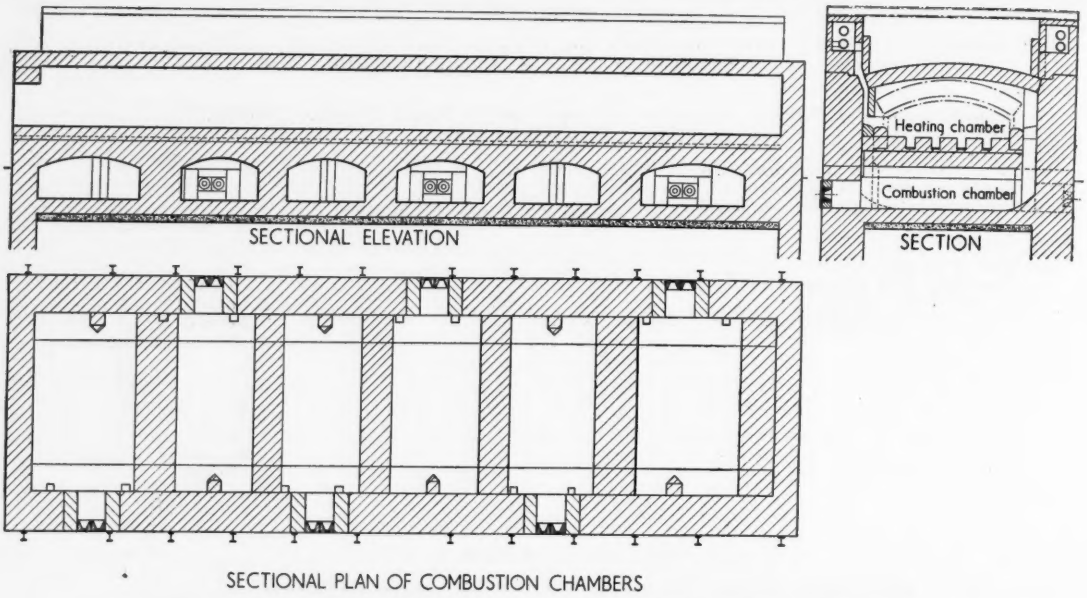


Fig. 11—Oil-fired tube-reheating furnace provided with under-hearth firing

FUTURE DEVELOPMENTS

The vital aspect of any industrial reheating furnace is obviously the provision of stock correctly heated and supplied at the rate required for the operation. However, an important secondary item is the possibility of recovering the waste heat for return direct to the furnace. Thereafter, any further heat recovery will depend on the prevailing circumstances.

At present the useful employment of the energy in the coal supplies in Britain does not exceed 15–20%.* By extending the practice of waste-heat recirculation, it might be possible to increase this figure to 30–35% for industrial furnaces—a very useful contribution to the national effort, when it is remembered that every additional 5% improvement means a saving in coal of nearly 70 million tons per year.

* E. W. Smith, "Waste-Heat Recovery from Industrial Furnaces," p. vii (Foreword): 1948, London, Chapman and Hall, Ltd.

Table V

ESTIMATED HEAT RECOVERY IN A CONTINUOUS-SLAB REHEATING FURNACE. STEAM RAISING *VERSUS* AIR RECUPERATION

(Based on Hot Charging, Gas Temperature from Furnace about 1000° C.)

	Recuperators		Air Dilution to Safeguard I.D. Fan		Boilers		I.D. Fan, h.p.	Fuel Consumption at Furnace, gal./hr.	Fuel Saved by Boiler, gal./hr.	Net Fuel Consumption, gal./hr.
	Air Preheat, °C.	Effluent Waste-Gas Temp., °C.	Air Fan, h.p.	Effluent Waste-Gas Temp., °C.	Steam Raised,* lb./hr./ Furnace	Effluent Waste-Gas Temp., °C.				
Alternative 1 Recuperator (capacity limited by available site) and I.D. fan	575	750	15	525	...	...	150	700	...	700
Alternative 2a Recuperator (limited capacity) and waste-heat boiler and I.D. fan	575	750	...	...	23,000	320	170	700	290	410
Alternative 2b Waste-heat boiler of limited capacity and recuperator and I.D. fan	500	525			10,500	800	210	730	130	600
Alternative 3 Waste-heat boiler and I.D. fan	...	...	...	...	34,000	320	75	820	430	390

*Steam at 200 lb./sq. in., 100° F. superheat

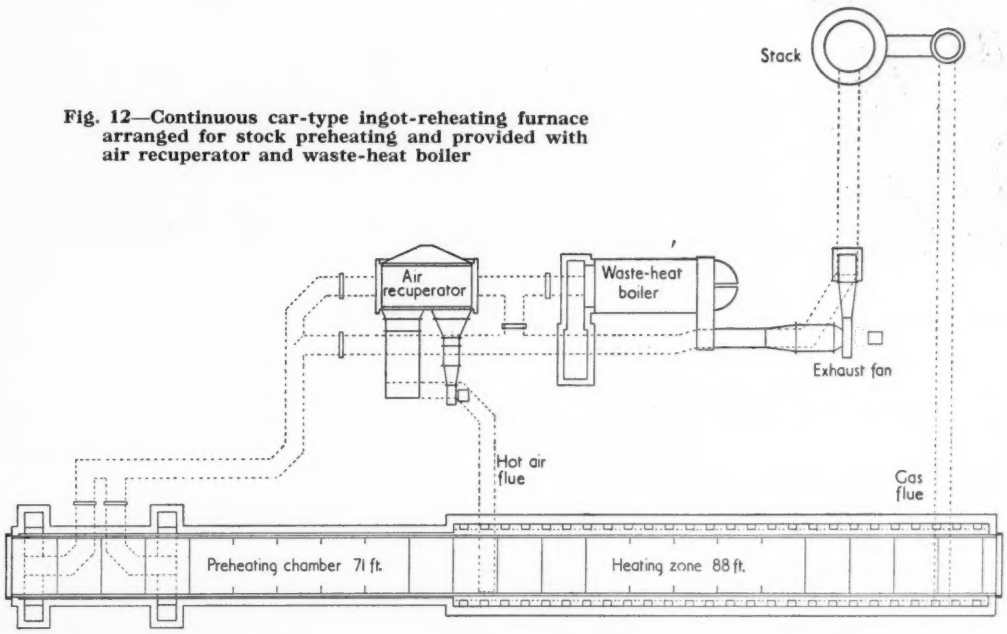


Fig. 12—Continuous car-type ingot-reheating furnace arranged for stock preheating and provided with air recuperator and waste-heat boiler

Thus, it is probably inevitable that as the available sources of energy become less, engineers may be forced, in the national interest, to extend the field of waste-heat recovery.

For the greatest efficiency, this secondary waste heat requires continuous operation, and inside the plant the raising of process steam and hot water seems to be the obvious development. Space heating, being a seasonal load in most cases, is not a good answer to the problem. Many works require dryers of some kind for cement, refractories, and so on, and these enable by-product steam to be used effectively.

The only other means whereby steam can be used continuously and with advantage appears to be in power generation. Attention has been focused on this recently, for it undoubtedly offers many possibilities, particularly where the steam from a number of furnaces can be combined. If necessary, the steam could be superheated by additional fuel burning, so that the most efficient power-generation equipment could be employed. Electric power of this type can be used for 'topping-up' plant requirements, or for supplementing the Grid.

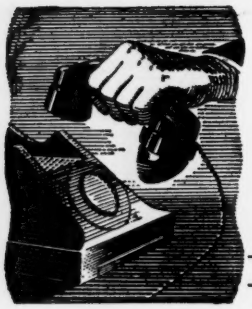
Attention has also been drawn recently to the possibility of employing the gas turbine for utilizing the products of combustion from furnaces. The published data seem to show an increased efficiency over the steam turbine under similar waste-heat conditions.*

Outside the plant, there is a striking demand for waste heat converted into a form that would enable it to be used for district heating and other similar projects. The Pimlico scheme, utilizing waste heat from Battersea power station, is an excellent example of what can be done in this direction. Where industrial furnaces are concerned, however, it is doubtful whether the use of waste heat for district heating is a good proposition as (*a*) the furnaces might be in operation when the heating load was light, *i.e.* during the summer months, or (*b*) the heating load might be heavy when the furnaces were off. It seems, therefore, that extended utilization of industrial waste heat outside the plant will have to develop along the lines of raising process steam for continuous consumption, such as in laundries and other continuous users of low- and medium-pressure steam and hot water.

Acknowledgments

The authors are grateful to the Wellman Smith Owen Engineering Corporation, Ltd., Salem Engineering Company, Ltd., Priest Furnaces, Ltd., and Incandescent Heat Company, Ltd., for allowing them to publish drawings, and also to Stewarts and Lloyds, Ltd., for permission to publish the information given in the paper.

* W. Gregson : *loc. cit.*, pp. 165–191.



NEWS

ANNOUNCEMENTS AND NEWS OF SCIENCE AND INDUSTRY

THE IRON AND STEEL INSTITUTE

Autumn General Meeting, 1952

The **Autumn General Meeting** of the Institute will be held on **Wednesday and Thursday, 26th and 27th November, 1952,** at 4 Grosvenor Gardens, London, S.W.1.

The programme is as follows:

Wednesday, 26th November

Morning Session

10.0 A.M. to 11.30 A.M.—Discussion on:

"*Cooling of Rimming Steel Ingots in a Casting Pit*," by R. T. Fowler and L. H. W. Savage (*July*, 1952). This paper will be discussed in conjunction with the following papers on Ingot Heat Conservation:

"*Time Studies from Casting to Rolling*," by A. V. Brancker, J. Stringer, and L. H. W. Savage (*Jan.*, 1950);

"*Mould and Ingot Surface Temperatures Measurements*," by A. V. Brancker (*July*, 1950), and

"*Ingot Mould Temperature Measurements*," by R. T. Fowler and J. Stringer (*March*, 1951)

(11.30 A.M. to 12.0 NOON—Interval)

12 NOON–1.15 P.M. The Thermodynamics of Steelmaking. The paper "*Development of Active Mixer Practice at Appleby-Frodingham*," by A. Jackson (*Oct.*, 1952) will be introduced by the author.

Following this, Dr. F. D. Richardson will act as rapporteur for the following group of papers:

"*Thermodynamic Properties of Silicon Monoxide*," by N. C. Tombs and A. J. E. Welch (*Sept.*, 1952)

"*Phosphorus–Oxygen Equilibrium in Liquid Iron*," by J. B. Bookey, F. D. Richardson, and A. J. E. Welch (*Aug.*, 1952)

"*The Free Energies of Formation of Tricalcium and Tetracalcium Phosphates*," by J. B. Bookey (*Sept.*, 1952)

"*The Free Energy of Formation of Magnesium Phosphate*," by J. B. Bookey (*Sept.*, 1952)

"*Effect of Sodium Oxide Additions to Steelmaking Slags: II—Dephosphorization of Steel by Soda Slags*," by W. R. Maddocks and E. T. Turkdogan (*June*, 1952)

"*Phase Equilibrium Investigation of the Na_2O–P_2O_5–SiO_2 Ternary System*," by E. T. Turkdogan and W. R. Maddocks (*Sept.*, 1952)

"*Thermodynamics of Substances of Interest in Iron and Steel Making: III—Sulphides*," by F. D. Richardson and J. H. E. Jeffes (*June*, 1952)

"*A Stoichiometric Combustion Method for the Determination of Sulphur in Slags*," by C. J. B. Fincham and F. D. Richardson (*Sept.*, 1952)

"*The Thermodynamic Calculation of Slag Equilibria*," by H. Flood and K. Grjotheim (*May*, 1952)

1.15 P.M.–2.30 P.M. Buffet Luncheon

Afternoon Session

2.30 P.M. to 5.0 P.M.—Discussion on Thermodynamics of Steelmaking

Thursday, 27th November

Morning Session

10.0 A.M. to 11.0 A.M.—Discussion on:

"*Addition of Boron to Steel by Reduction from Boron Oxide*," by G. E. Speight (*June*, 1952)

(11.0 A.M. to 11.15 A.M.—Interval)

11.15 A.M. to 12.15 P.M.—Discussion on:

"*Effect of Direction of Rolling and Straining and of Ageing on the Mechanical Properties of a Mild-Steel Plate*," by C. F. Tipper (*Oct.*, 1952)

12.15 P.M. to 1.15 P.M.—Discussion on:

"*Rapid Softening of Cold-Drawn Austenitic Stainless Steels*," by F. A. Hodierne and C. E. Homer (*July*, 1952)

The President

The President of The Iron and Steel Institute, Captain H. Leighton Davies, C.B.E., was elected an Honorary Member of The Chemical, Metallurgical, and Mining Society of South Africa, for the year ending 30th June, 1953, at a meeting of the Council of the Society held on 8th August, 1952.

NEWS OF MEMBERS

▶ Mr. C. ANDERSON has transferred from Messrs. Stewarts and Lloyds Ltd. at Corby to their Bilston Works. Last year he was awarded the degree of M.A. of Cambridge University.

▶ Mr. K. J. A. BROOKES has been awarded the degree of B.Sc. (Eng. Met.) of London University. He is now Works Metallurgist to R. G. McLeod Ltd., London.

▶ Mr. G. H. CLUBLEY has left Jarrow Metal Industries, Ltd., to take up an appointment as Assistant General Manager to E. Jopling and Sons, Sunderland.

▶ Mr. R. I. LINDSAY has left the G.K.N. Group Research Laboratory and has been appointed Chief Metallurgist to Messrs. Joseph Sankey and Sons Ltd., Bilston, Staffs.

▶ Mr. N. H. SOWARD, of the Wellman Smith Owen Engineering Corporation Ltd., has transferred to Wellman Smith Owen of South Africa (Pty.) Ltd., Johannesburg.

▶ Mr. J. E. SRAWLEY has left the British Cast Iron Research Association and is now residing in Texas, U.S.A.

Obituary

Monsieur EMILE A. HALET, Chevalier de l'Ordre de Léopold, President of the Belgian Chamber of Commerce in London, on 24th June, 1952.

Mr. EMRYS WILLIAMS, of Gowerton, Glam., on 23rd June, 1952.

CONTRIBUTORS TO THE JOURNAL

S. A. Burke, B.Sc., Ph.D.—Principal Scientific Officer, British Coal Utilisation Research Association. Dr. Burke was educated at Wrekin College, and obtained the B.Sc. degree at the Royal College of Science in 1932. In 1936 he was awarded the Diploma of Imperial College and the degree of Ph.D. He then joined the British Aluminium Company as a Research Chemist at their Burntisland Works, Fifeshire. After two years in the laboratory, he began chemical engineering research on new plants in the works, and in 1939 he was appointed a Shift Superintendent at their new Alumina Factory at Newport, Monmouthshire.

S. A. Burke

From 1942 until 1946 Dr. Burke was with the Aluminium and Pressure Vessel Company, where he developed a new entrainer process and was loaned to the Ministry of Supply for one year, to start up and supervise the first full-scale multi-stage distillation plant. In 1946 he joined the British Coal Utilisation Research Association, where he is now working on a project designed to bring the performance of gas producers more into line with that of other chemical engineering equipment, and to develop new types of gasification plant.

G. A. Sparham, B.Sc.(Eng.), A.M.I.Mech.E., A.M.Inst.F.—Senior Scientific Officer, Gas Producer Department, British Coal Utilisation Research Association. Mr. Sparham was educated at Alfreton Swanwick Hall County Secondary School; he graduated from the City and Guilds College in 1940 with the degree of B.Sc. (Hons.) in mechanical engineering. From 1940 to 1946 he served as an engineer officer in the Royal Air Force, engaged on duties involving the inspection and maintenance of squadron aircraft and associated equipment. On his demobilization he joined the staff of the British Coal Utilisation Research Association, where he has been studying problems of gas-producer research. He is now in charge of a Section investigating methods of utilizing colliery slurry.

A. W. Leadbeater, A.M.I.E.E.—Steel Works Electrical Engineer at Stewarts and Lloyds Ltd., Corby. Mr. Leadbeater was educated at Castleton Senior School, and received his technical training at the Rochdale Technical School and at the Manchester College of Technology, where he was granted the Associateship of the College in 1928. His practical training commenced at Whipp and Bourne Ltd., switchgear manufacturers, and was completed at the Metropolitan-Vickers Electrical Company, Manchester, which he joined in 1929 as Switchgear Test Engineer. After transferring to the Plant Engineering Staff, he took charge of the Experimental Laboratory attached to the Large Machine Test. There he specialized in rolling-mill equipment, turbo-alternator balancing, and the mechanical design of large electrical machinery. In 1940 he joined the staff of Stewarts and Lloyds Ltd. as Assistant Works Electrical Engineer; he took up his present appointment in 1949. Mr. Leadbeater serves on the Electrical Engineering Committee of the British Iron and Steel Research Association.

R. B. Jamieson—Assistant Works Electrical Engineer at Stewarts and Lloyds Ltd., Corby. Mr. Jamieson received his technical training at Bellshill Academy and at Coatbridge Technical College. His practical training commenced at the Mossend Works of Stewarts and Lloyds Ltd., and covered the maintenance of rolling mills and melting shops. When the firm transferred to Corby in 1933, Mr. Jamieson became responsible for the installation of the large rolling-mill gear, which was accomplished in record time. He was in charge of the electrical maintenance of all rolling mills from 1938 until 1949, when he took up his present appointment. Mr. Jamieson has had 33 years' experience of the steel industry.

D. F. Arthur

G. M. Leak

D. F. Arthur, B.Sc.—Member of the Cold-Rolling Research Team of the British Iron and Steel Research Association. Mr. Arthur was born in 1925, and was educated at Swansea Grammar School and at University College, Swansea, where he graduated with 1st Class Honours in Physics in 1950. He served in the Royal Air Force from 1944 to 1948, and took up his present appointment in 1950.

G. M. Leak, B.Sc., Ph.D.—Metallurgy Division, British Iron and Steel Research Association. Dr. Leak was born in Hull in 1921. He was educated at Hull Grammar School and Thames Valley School, Twickenham, and at Queen Mary College, University of London, where he graduated with the degree of B.Sc. (Hons. Phys.) in 1942. For the next five years he served as an officer with R.E.M.E. in Britain and Europe. In 1947 he joined B.I.S.R.A. as a Technical Secretary in the Metallurgy Division. He was seconded in 1949 to Birmingham University, where he worked under Professor Cottrell in the Department of Theoretical Metallurgy, investigating strain ageing in iron. He was awarded a Ph.D. degree in 1951. On leaving Birmingham he transferred to the new laboratories of the Metallurgy Division of B.I.S.R.A., in Sheffield.

BRITISH IRON AND STEEL RESEARCH ASSOCIATION

Conference on the Design of Rolling Mills

The full report of the Plant Engineering Division's Seventh Conference, held in London on 13th and 14th June, 1951, has recently been published by the Association. The Report covers mechanical engineering aspects of rolling-mill design. Descriptions are given of the new 42-in. slabbing mill of John Summers and Sons Ltd., at Shotton; new blooming and billet mills at the Normanby Park Steel Works of John Lysaght's Scunthorpe Works Ltd.; a rod and bar mill at Guest, Keen and Nettlefolds (South Wales) Ltd., Cardiff; and the light section mill at the Darlington Works of the Darlington and Simpson Rolling Mill Co., Ltd. In the discussion, reference is made to operating experiences, and possible lines of future research and development are indicated.

The Report, which contains 51 pp. and 17 illustrations, can be obtained from B.I.S.R.A., 11 Park Lane, London, W.1, at 10*s.* per copy, post free.

IRON AND STEEL ENGINEERS GROUP

The **Twentieth Meeting** of the Group will be held in London on **Thursday, 11th December, 1952.** At the Morning Session three papers will be presented for discussion: "Experiences with the Escher Metallic Recuperator on High-Temperature Furnaces," by H. Escher (Sept. 1951, pp. 39–46); "Trials of an Experimental Austeel-Escher Metallic Recuperator," by J. B. Davis, W. Ernest, and H. Kay (Jan. 1951, pp. 66–70); and "Heat Recovery in Industrial Furnaces," by A. Clift and C. Knight (*this issue*, pp. 327–339).

At the Afternoon Session, discussion will be devoted to the paper "Fuel Applications in Open-Hearth Furnaces," by D. F. Marshall (*this issue*, pp. 315–326).

AFFILIATED LOCAL SOCIETIES

Newport and District Metallurgical Society

The list of Officers for the Session 1952–53 is as follows:

President

G. H. LATHAM, LL.D.

Hon. Treasurer

J. E. WELLS, B.Sc., A.Met.

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E. E. THOMPSON, B.Sc.
15 Bassaleg Road, Newport.

NEWS OF SCIENCE AND INDUSTRY

Symposium on Caustic Cracking in Steam Boilers

A symposium on "Caustic Cracking in Steam Boilers" arranged by the Corrosion Group of the Society of Chemical Industry, will be held on Thursday, 20th November, 1952, at the Institution of Mechanical Engineers, Storey's Gate, St. James's Park, London, S.W.1. Arrangements have been made for a buffet luncheon.

Advance summaries of papers to be presented at the Symposium will be forwarded, post free, on application to the Assistant Secretary, Society of Chemical Industry, 56 Victoria Street, London, S.W.1.

DIARY

6th Nov.—LEEDS METALLURGICAL SOCIETY—"*The Production of Some of the Rarer Metals*," by A. R. Powell—Chemistry Department, The University, Leeds 2, 7.0 P.M.

12th Nov.—SWANSEA AND DISTRICT METALLURGICAL SOCIETY (Joint Meeting with The Iron and Steel Institute)—"*The Variation in Electrical Properties of Silicon–Iron Transformer Sheets*," by S. Rushton and D. R. G. Davies—Central Library, Swansea, 6.30 P.M.

12th Nov.—THE NEWCOMEN SOCIETY—Annual General Meeting—"*Earl of Dudley's Level New Furnaces at Brierley Hill*," by T. M. Hoskison—The Iron and Steel Institute, 4 Grosvenor Gardens, London, S.W.1—5.30 P.M.

13th Nov.—LIVERPOOL METALLURGICAL SOCIETY (Joint Meeting with Institute of Welding)—"*The Heat-Treatment of Steels*," by R. J. Brown—Liverpool Engineering Society, The Temple, Dale Street, Liverpool, 6.30 P.M.

13th Nov.—STAFFORDSHIRE IRON AND STEEL INSTITUTE—"*Recent Progress in Basic Open Hearth Furnaces and Practice*," by W. Angus—Training Centre, Stewarts and Lloyds Ltd., Bilston, 7.30 P.M.

14th Nov.—INSTITUTION OF MECHANICAL ENGINEERS—Thomas Hawksley Lecture: "*The Mechanism of Work-Hardening in Metals*," by N. F. Mott, F.R.S.—Storey's Gate, St. James's Park, London, S.W.1, 5.30 P.M.

18th Nov.—SHEFFIELD SOCIETY OF ENGINEERS AND METALLURGISTS (Joint Meeting with the Sheffield Metallurgical Association and the Institute of Metals)—"*Metallurgical Problems Arising from Stratospheric Flight*," by P. L. Teed—Grand Hotel, Sheffield, 7.0 P.M.

19th Nov.—INSTITUTE OF METALS—Symposium on "*Properties of Metallic Surfaces*"—4 Grosvenor Gardens, London, S.W.1, 9.45 A.M.

19th Nov.—NORTH WALES METALLURGICAL SOCIETY—"*The Metallurgy of Welding*," by W. I. Pumphrey—County Primary School, Plymouth Street, Shotton, Nr. Chester, 7.15 P.M.

20th Nov.—SOCIETY OF CHEMICAL INDUSTRY (Corrosion Group)—Symposium on "*Caustic Cracking in Steam Boilers*"—Institution of Mechanical Engineers, Storey's Gate, St. James's Park, London, S.W.1, 9.45 A.M.

21st Nov.—INSTITUTION OF MECHANICAL ENGINEERS—"*Recent Developments in the Machinability of Steel*," by K. J. B. Wolfe—Storey's Gate, St. James's Park, London, S.W.1, 5.30 P.M.

25th Nov.—SHEFFIELD METALLURGICAL ASSOCIATION—"*Survey of the Use of the Direct Reading Spectrometer in America*," by G. E. Speight—Grand Hotel, Sheffield, 7.0 P.M.

26th–27th Nov.—THE IRON AND STEEL INSTITUTE—Autumn General Meeting—4 Grosvenor Gardens, London, S.W.1.

27th Nov.—STAFFORDSHIRE IRON AND STEEL INSTITUTE—"*Some Recent Engineering Developments in the Iron and Steel Industry*," by J. A. Kilby—Station Hotel, Dudley, 7.30 P.M.

TRANSLATION SERVICE

(The previous announcement was made in the October, 1952, issue of the *Journal*, p. 230.)

TRANSLATION AVAILABLE

No. 455 (German). O. J. STEBEL: "Design and Operation of Modern Soaking Pits." (*Stahl und Eisen*, 1952, vol. 72, Apr. 24, pp. 466–474).

TRANSLATIONS IN COURSE OF PREPARATION

(German). G. HARTLIEF and H. KORNFELD: "Spectro-Analytical Rapid Determinations on Metallurgical Slags." (*Archiv für das Eisenhüttenwesen*, 1952, vol. 23, Mar./Apr., pp. 107–110).

(German). H. WERWACH: "Use of Dolomite Bricks in the Electric Arc Furnace when Refining with Oxygen." (*Stahl und Eisen*, 1952, vol. 72, Mar. 27, pp. 341–345).

CHARGES FOR COPIES OF TRANSLATIONS—The charge for translations is £1 for the first copy and 10*s.* for each additional copy of the same translation. Requests should be accompanied by a remittance. These translations are not available on loan from the Joint Library.

ABSTRACTS OF CURRENT LITERATURE *and* BOOK NOTICES

IRON AND STEEL MANUFACTURE AND RELATED SUBJECTS

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MINERAL RESOURCES

Contribution to the Knowledge of Saar-Lorraine Coals. E. Hoffmann. (*Glückauf*, 1952, **88**, Apr. 26, 434–448).

An Appraisal of Minable Coking-Coal Reserves in the United States. A. E. Toenges. (*Amer. Inst. Min. Met. Eng., Blast Furn., Coke Oven, Raw Mat. Conf.*, 1951, Apr., 45–53). The study of the reserves of coking coal, and the investigation of methods of upgrading and blending to improve carbonizing properties are outlined. Estimates of recoverable reserves in four counties of Pennsylvania are given, and methods of assessment are described.—T. E. D.

Manganese from Morocco. G. Joutel. (*Mét. Constr. Méc.*, 1952, **84**, Apr., 271). This brief note describes the Moroccan manganese deposits at Oudjda, Bou Arfa, and Aoulou in the southern Atlas mountains, and in the Imini region. Morocco offers good possibilities as a source of manganese.—R. S.

Cobalt: World Sources and Supplies. J. B. Richardson. (*Times Review of Industry*, 1952, **6**, July, 24–27).

Mineral Status of the Far East. Kung-Ping Wang. (*Min. Eng.*, 1951, **3**, Nov., 943–948; Dec., 1049–1055). The mineral potential of the Far East is presented and the production and resources of each of the Far Eastern countries are discussed.—H. F.

ORES—MINING AND TREATMENT

The Italian Mineral Industry. A. Giordano. (*Min. Mag.*, 1951, **85**, Dec., 347–349). This is an account of recent progress in the Italian mineral industry showing the parts played by State and private enterprise. The minerals surveyed are bauxite, antimony, iron, manganese, mercury, lead, zinc, asbestos, sulphur, and marble.—H. F.

Rock Drilling with Hard Metal Bits. T. Ekstam and E. Ryd. (*Jernkontorets Ann.*, 1952, **136**, Mar., 41–58). [In Swedish].

New Mining Methods Tested by Menominee Range Iron Ore Producers. P. D. Pearson and W. W. Jamar. (*Trans. Amer. Inst. Min. Met. Eng.*, 1951, **190**, 337–346). The authors have assembled information from all the iron-ore mining operations in the Menominee Range in Michigan. Some of the changes made and trends towards future changes in methods of drilling, transportation, and mining are reviewed.—G. F.

Production Jet-Piercing of Blastholes in Magnetic Taconite. D. H. Fleming and J. J. Calaman. (*Trans. Amer. Inst. Min. Met. Eng.*, 1951, **190**, 585–591). A jet-piercing process is in commercial use for piercing primary blast-holes in magnetic taconite at the Erie Mining Co., Minnesota. The theory of the process, important features of the latest machines, and actual results obtained, are described in detail.—G. F.

The Metal Mining Industry in Japan. R. Y. Grant. (*Trans. Amer. Inst. Min. Met. Eng.*, 1951, **190**, 953–960; *Min. Eng.*, 1951, **3**, Nov., 953–960). The history of metal mining in Japan and the characteristics of the industry as they existed from 1925 to 1945 are outlined.—G. F.

Developing Mesabi Orebodies Under Lake Beds. J. R. Stuart. (*Trans. Amer. Inst. Min. Met. Eng.*, 1951, **190**, 777–779). Basing his observations on experiences during the development of the Embarrass Mine, part of which lay under the Syracuse Lake in the Mesabi Range, the author describes considerations which have to be taken into account in developing ore deposits under lake beds.—G. F.

Swedish Ore Mining—Impressions from a Visit. F. Benthaus. (*Glückauf*, 1952, **88**, June 7, 560–579).

Mechanical Handling and Movement of Iron Ores in Germany. A. E. Lance. (*Min. Mag.*, 1951, **85**, July, 9–18; Aug., 81–85). Operations in German mines, in the open-cast, and at harbours in the movement of iron ore are described.—R. A. R.

Oliver Hauls Overburden with Conveyor Belts. J. K. Lovrien. (*Min. Eng.*, 1951, **3**, Dec., 1054–1055). A description is given of the link-belt roller-bearing conveyor system, recently installed at the Gross-Marble Mine of the Oliver Iron Mining Co. at Marble, Minn., U.S.A., for removal of overburden and lean ore.—H. F.

The Agglomeration of Taconite Concentrate. M. A. Kunde. (*Mines Mag.*, 1952, **42**, May, 39–52). Recent research and commercial developments of new and improved methods of agglomeration of taconite are described. These processes are pelletization, sintering, nodulizing, and briquetting.—H. F.

Taconites *beyond* Taconites. N. M. Levine. (*Min. Eng.*, 1952, **4**, Apr., 361–363). A survey is made of future prospects for possibilities, as indicated by present-day research, of industrial treatment of non-magnetic taconites.—H. F.

New Developments in Ore Dressing in 1951. J. J. Bean. (*Mines Mag.*, 1952, **42**, Mar., 51, 52, 68). This is a review of major new developments in mineral dressing. It includes beneficiation, cyclone separation, magnetic separation, flotation plants, and fine concentrate treatment.—H. F.

The Third Theory of Comminution. F. C. Bond. (*Min. Eng.*, 1952, **4**, May, 484–494). A completely new theory of comminution has been derived, namely, that the total work

required for grinding and crushing varies inversely with the product size. When the work input, feed size, and product size of one reduction are known, the theory permits rapid calculation of the work required for any size reduction and permits direct comparisons of efficiency. The relative efficiencies of different operations and circuits involving different feed and product sizes can be compared directly, and, after laboratory tests have been made, the relative efficiencies of plants reducing various materials can be compared.—H. F.

Some Newer Thoughts on Grinding. C. E. Golson. (*Mines Mag.*, 1952, **42**, Feb., 16–22). The author discusses ore-grinding problems in the U.S.A. and Mexico. These problems are: Grinding considered as part of a metallurgical process; overgrinding; classification considered as an integral part of grinding; high-speed *versus* low-speed grinding; ball migration as a factor in grinding; rod milling; liners in relation to grinding; and corrosion and abrasion in relation to the wear of grinding media.—H. F.

Basic Laboratory Studies in the Unit Operation of Crushing. J. W. Axelson, J. T. Adams, jun., J. F. Johnson, J. N. S. Kwong, and E. L. Piret. (*Trans. Amer. Inst. Min. Met. Eng.*, 1951, **190**, 1061–1069). A description is given of laboratory studies of crushing operations in which the energy input has been related to the amount of new surface of the crushed material produced. Earlier work on the mechanism of the fracture process and on the particle-size distribution of the crushed product is reviewed.—G. F.

Jaw Crusher Capacities, Blake and Single-Toggle or Overhead Eccentric Types. D. H. Gieskieng. (*Trans. Amer. Inst. Min. Met. Eng.*, 1951, **190**, 971–974). The author describes an investigation of the apparent and actual capacities of jaw-crushers of the Blake, single-toggle, and overhead eccentric types.—G. F.

Notes on Cyclone Practice in the United States. P. W. Sherwood. (*Min. Mag.*, 1952, **86**, Apr., 210–213). Important economies can be effected in classification and thickening operations by use of the new D.S.M. liquid cyclones. These tools have made inroads into the coal-cleaning industry and are used for the beneficiation of iron ore. Some American applications of these machines, including operation and performance, are discussed.—H. F.

Precision in Mineral Separation. T. Eder. (*Min. Mag.*, 1952, **86**, Apr., 205–208). This is a mathematical dissertation discussing the choice of a single coefficient as an aid to international standardization, covering the whole field of mineral dressing.—H. F.

Vanadium Enrichment During Beneficiation of Oolitic Iron Ores. H. Kirchberg. (*Z. Erz. u. Met.*, 1951, **4**, Mar., 98–104). The occurrence of vanadium in iron ores of magmatic and sedimentary origin is surveyed. Studies of vanadium enrichment in various German ores are described. No positive results are reached, but the author sees future possibilities in selective mining followed by wet classification.—E. C.

Investigation of the Effect of Ultrasonic Waves on Ore Flotation. W. Petersen. (*Z. Erz. u. Met.*, 1951, **4**, Feb., 62–68; Apr., 142–148). Dispersing, emulsifying, and flocculating effects of ultrasonic waves are described. The theory of applications of ultrasonics to ore beneficiation is outlined. Experimental magnetostriction and crystal oscillator transmitters were used for flotation trials on various ores, and results are discussed. It was possible to improve selectivity of the flotation agents for sulphide ores and to raise ore recovery. Practical applications are not yet possible because of low capacity and high initial and operating costs of the equipment.—E. C.

Development of Modern Impeller Flotation Machines. G. Quittkat. (*Z. Erz. u. Met.*, 1950, **3**, Aug., 257–264). British, German, and American flotation machines are described and their relative merits discussed. The author prefers Denver-type cells for differential flotation and for difficult ores, and recommends Fagergren-type cells for single-product flotation. E. C.

A Strength Test for Sinter. R. W. Hyde. (*Amer. Inst. Min. Met. Eng., Blast Furn., Coke Oven, Raw Mat. Conf.*, 1951, Apr., 141–152). The screen distribution after a 4-ft. free drop of a 10–12 lb. sample on to a steel plate is used as a measure of the degradation liable to occur with handling.—T. E. D.

Physical Tests and Results on Some Agglomerated Iron Ores. F. M. Hamilton. (*Amer. Inst. Min. Met. Eng., Blast Furn., Coke Oven, Raw Mat. Conf.*, 1951, Apr., 135–140). A laboratory crush test and a modified tumbler test were used on sinters and pellets, and a reducibility test with hydrogen at 800° C. is also described.—T. E. D.

Testing of Iron Ores and Agglomerates Related to Blast-Furnace Use. R. J. Morton. (*Amer. Inst. Min. Met. Eng., Blast Furn., Coke Oven, Raw Mat. Conf.*, 1951, Apr., 122–134). The A.S.T.M. coke tumbler test is used for ores, sinters, and other agglomerates. Results are given for this test when used for ores, sinters, pellets, and briquettes. The coke shatter test is modified so that it is more severe. The limited use of the compressive strength test is mentioned. A decrepitation test has been developed and the S.K. (South Kensington) porosity test has been modified to use helium and mercury. Certain correlations are made between various properties. The discussion is reported.—T. E. D.

Design of a Recent Sintering Plant. C. G. Bigelow. (*Amer. Inst. Min. Met. Eng., Blast Furn., Coke Oven, Raw Mat. Conf.*, 1951, Apr., 103–110). A 2400-tons/day plant is described. It consists of two 72 in. by 118 ft., 16-windbox, Dwight-Lloyd-type machines. The materials flow diagram is shown, the main raw material being flue dust.—T. E. D.

Recent Advances in Sintering-Plant Design and Operation. M. F. Morgan. (*Amer. Inst. Min. Met. Eng., Blast Furn., Coke Oven, Raw Mat. Conf.*, 1951, Apr., 91–103). Improvements have been chiefly in the design of auxiliaries. An adjustment control of the expansion gap at the strand discharge end is described and illustrated. The layout of a windbox downcomer with expansion joints is shown, and details of the shear-pin assembly and an arrangement for a safety limit switch are given. Pug mills, swinging spouts, and aerated feed hoppers are mentioned. Ignition hood design and rotary coolers are discussed in some detail.—T. E. D.

Iron-Ore Beneficiation—Present Practices and Trends. S. E. Erickson. (*Amer. Inst. Min. Met. Eng., Blast Furn., Coke Oven, Raw Mat. Conf.*, 1951, Apr., 4–18). Flow schemes in the preparation of merchantable ore, wash ores, intermediate ore, and both magnetic and non-magnetic taconites are described and explained. Modern trends in methods of treatment are mentioned.—T. E. D.

Concentration of Iron Ores: Importance of Systematic Investigations for Continental Methods of Beneficiation. L. Cocke, D. Dastillon, and L. Wahl. (*Iron Coal Trades Rev.*, 1952, **164**, May 2, 973–974, 976). The authors discuss the importance of systematic studies of the chemical composition and crystalline structure of iron ores in the selection of an economic process of beneficiation. Brief descriptions are given of laboratory methods of ore concentration which have been based on ore characteristics, and an indication is given of the limitations and practical possibilities of these methods.—G. F.

Cheaper and Improved Methods of Beneficiating Lower Grade Ores Was Target in 1951. R. E. Byler. (*Min. Eng.*, 1952, **4**, Feb., 149–155). A description is given of the work done in the U.S.A. during 1951 on mineral beneficiation projects of great number and variety with new and improved methods and equipment. The subjects covered include iron range developments, grinding, screening, size classification, materials handling, and gravity concentration.—H. F.

FUEL—PREPARATION, PROPERTIES, AND USES

Flame-Radiation Experiments: Various Fuels Investigated in International Research. (*Iron Coal Trades Rev.*, 1952, **164**, June 27, 1441–1443). A further progress report is given of the flame-radiation experiments being carried out by an international group at Ijmuiden, Holland. Problems being studied are outlined, and brief details of work carried out on oil burners are given.—G. F.

Some Recent Advances in Fuel Technology. C. C. Hall. (*Blast Furn. Steel Plant*, 1952, **40**, May, 542–545). Recent work carried out at the Fuel Research Station, Greenwich, is reported. The work described includes the study of external deposits on water-tube boilers, the use of coal in gas turbines, carbonization and gasification of low-grade coals, and the production of producer gas from coke breeze.—B. G. B.

Thermal Metamorphism and Ground Water Alteration of Coking Coal near Paonia, Colorado. V. H. Johnson. (*Min. Eng.*, 1952, **4**, Apr., 391–395). A survey of an area near Paonia showed large quantities of non-coking coal lying next to coking coal in the same coal beds.—H. F.

Efficiency and Sharpness of Separation in Evaluating Coal-Washery Performance. H. F. Yancey and M. R. Geer. (*Trans. Amer. Inst. Min. Met. Eng.*, 1951, **190**, 507–517). Criteria for evaluating the performance of coal washeries are

discussed critically, with particular reference to ideas developed at the First International Conference on Coal Preparation held in Paris in June 1950. Criteria relating only to the sharpness with which a cleaning unit separates coal from impurities are compared with those which reflect loss of coal or quality of products.—G. F.

Coal Preparation in England and Holland. J. Griffen. (*Trans. Amer. Inst. Min. Met. Eng.*, 1951, **190**, 183–184). Methods of coal preparation in use in England are briefly compared with methods used in America. Problems of the Dutch coal industry are also summarized.—G. F.

Froth-Flotation of Coal. E. Hindmarsh and P. L. Waters. (*Trans. Mining Institute of Scotland*, 1952, **65**, Part 3, 147–161).

Modern Construction of Coke Ovens. H. G. Reyes. (*Met. Elect.*, 1951, **15**, July, 36–42; Aug., 38–44; Oct., 40–46). [In Spanish]. The refractories for constructing coke ovens are discussed. The effects of differential expansion between bricks and between walls, and devices for taking up expansion are described. The advantages of the free-expansion oven are stated.—R. S.

Using Electric Furnaces and Heaters to Determine the Free-Swelling Index of Coal. E. Swartzman and G. C. Behnke. (*Trans. Amer. Inst. Min. Met. Eng.*, 1951, **190**, 871–878). The authors propose a modification to the A.S.T.M. standard method of determining the free-swelling index of coal. An easily calibrated electric cone heater and platinum crucible are suggested in place of the specified gas burner and quartz crucible.—G. F.

Handling in Coke Ovens and Blast Furnaces. A. Firket. (*Rev. Univ. Min.*, 1952, 9th series, **8**, May, 131–138). A cost analysis is made of mechanical handling processes in the operation of coke ovens and blast-furnaces. A comparison is made of the number of operators per day, the energy consumed, and tons of minerals moved.—B. G. B.

Application of Automatic Control to the Coal Feed of Steelworks Gas Producers. S. A. Burke and G. A. Sparham. (*J. Iron Steel Inst.*, 1952, **172**, Nov., 257–264). [This issue].

Solvent Extraction of Phenol from Coal-Tar Hydrocarbons: The Use of Glycerol, Triethylene Glycol and Their Aqueous Solutions as Solvents. A. P. C. Cumming and F. Morton. (*J. Appl. Chem.*, 1952, **2**, June, 314–328).

Theoretical and Practical Aspects of Coal and Coke Testing. B. P. Mulcahy. (*Amer. Inst. Min. Met. Eng., Blast Furn., Coke Oven, Raw Mat. Conf.*, 1951, Apr., 54–89). Sampling location in coking and blast-furnace plants is discussed. Moisture, ash, sulphur, volatile matter, and screen analysis methods are surveyed. Shatter and tumbler tests are outlined, and the determinations of bulk density, specific gravity, porosity, ultimate analysis, and reactivity are mentioned. Coke evaluation formulæ are discussed. Similar tests on coal are reviewed, and coking characteristics are also discussed. The significance of the plasticity of coal and the effects of oxidation are indicated. The principles that should be involved in the selection of coals and cokes are emphasized.—T. E. D.

Low-Temperature Coke as a Reactive Carbon. C. E. Lesher. (*Amer. Inst. Min. Met. Eng., Blast Furn., Coke Oven, Raw Mat. Conf.*, 1951, Apr., 19–36; *Trans. Amer. Inst. Min. Met. Eng.*, 1950, **187**, 805–810). The reactivities of 850° F. and 1650° F. cokes, measured by their relative rates of reduction of iron oxides at temperatures up to 2200° F., have been studied. The cokes were made from a low-sulphur Pittsburgh coal, and Lake Superior brown ore was used. An ore–'Disco' agglomerate was also tested. The reduction test apparatus is described. The rate of heating of the ore–coke mixtures was about 10° F./min., reaching 2200° F. in 210 min.; the heated samples were continuously weighed. Percentage loss-in-weight curves, gas compositions, and material balances were studied. (18 references).—T. E. D.

Methane and Coal. Sir Alfred Egerton. (British Coal Utilisation Res. Assoc.: *Engineering*, 1952, **173**, May 30, 678–680). An abridged version is given of the First Coal Science lecture delivered to the British Coal Utilisation Research Association on May 7, 1952. The author discusses the origin of methane in coal, the extraction of methane, and the utilization of the energy of firedamp.—M. D. J. B.

Heat Recovery in Industrial Furnaces. A. Clift and C. Knight. (*J. Iron Steel Inst.*, 1952, **172**, 327–339). [This issue].

Particle Charging in Electrostatic Precipitation. H. J. White. (*Trans. Amer. Inst. Elect. Eng.*, 1951, **70**, Part II, 1186–1191). In unipolar corona discharge, the two particle-charging mechanisms of bombardment of the particles by ions moving in the D.C. field and attachment of ions to the particles by ion diffusion are discussed. Comparisons are drawn to substantiate theoretical and laboratory results. It is concluded that in electrostatic precipitation, the external field particle charging procedure is used for particles larger than about 0·5 microns in dia.; ion diffusion is for small particles. In practice, large quantities are considered together with certain abnormal charging conditions.—H. D. W.

Electrostatic Precipitation of High-Resistivity Dust. G. W. Penney. (*Trans. Amer. Inst. Elect. Eng.*, 1951, **70**, Part II, 1192–1195). The effects of high resistivity dust on the electrodes of an ionizer, resulting in partial neutralization of the charge acquired by the particles, are discussed. Equipment for measuring the resistivity of dust is described, and experiments carried out using fly ash on one precipitation cell. Reverse ionization is considered likely where the resistivity of the dust is greater than 10^{11} ohm-cm.—H. D. W.

Transient Flow in Gas Transmission Lines. R. H. Olds and B. H. Sage. (*Trans. Amer. Inst. Min. Met. Eng.*, 1951, **192**, 217–222). By means of two examples, the authors demonstrate the application of partial differential equations, describing the conservation of momentum and material, to the prediction of pressure distribution in long pipelines carrying gas under transient conditions.—G. F.

TEMPERATURE MEASUREMENT AND CONTROL

Recent Advances in Measuring Flame and Gas Temperatures. E. J. Burton and R. K. Hurden. (*Iron Coal Trades Rev.*, 1952, **164**, May 30, 1211–1215). In this review of the techniques of measuring flame temperatures, the authors consider the purpose of temperature measurement in flame-heated furnaces and examine whether any of the new developments may be applied in the steel industry. In investigating heat transfer from luminous flames, both flame temperature and emissivity need to be measured, and the Schmidt technique appears to be the most reliable and least difficult method for use on industrial furnaces. Instruments for measuring sensible heat content of gases have been developed, but the continuous metering of preheat temperatures above 800° C. requires further study.—G. F.

Heating and Temperature Control of Plating Solutions. W. E. Stadel and C. Civan. (*Amer. Electropl. Soc.*, 38*th Annual Proc., Tech. Sessions*, 1951, July–Aug., 63–66). The use of temperature control, when employing steam heating in plating baths, is briefly outlined.—T. E. D.

Flame Hardening: Temperature Control for Precision Work. (*Iron Steel*, 1952, **25**, Apr., 134). *See* 'Temperature Measurement in Flame-Hardening,' *J. Iron Steel Inst.*, 1952, **172**, Sept., 104.

Optical Temperature Scale and Emissivities of Liquid Iron–Copper–Nickel Alloys. D. B. Smith and J. Chipman. (*Trans. Amer. Inst. Min. Met. Eng.: J. Met.*, 1952, **4**, June, 643–644). The authors describe the determination of the emissivities of molten iron–nickel–copper alloys from temperature measurements taken simultaneously by optical pyrometer and by thermocouple. In each of the binary systems containing copper, maximum emissivity is found at about 20–30% Cu, and the maximum deviation from the simple law of mixtures occurs at about 50–60% Cu, probably because of an electronic effect.—G. F.

REFRACTORY MATERIALS

Refractory Concrete. A. E. Williams. (*Chem. Age*, 1952, **66**, May 10, 721–724). Refractory concrete is a mixture of coarse aggregate, fine aggregate, aluminous cement, and water. The aggregate must consist of known refractory materials such as firebrick for temperatures up to 1300° C., and magnesia, alumina, or zirconia for higher temperatures. It is employed extensively in many types of furnace, its good resistance to sudden temperature fluctuation, and its low thermal conductivity making it particularly useful.—T. E. D.

Investigation of System MgO–CaO–TiO_2–SiO_2. A. S. Berezhnoi. (*Ogneupory*, 1950, **15**, 11, 493–504).

Use of Dolomite Bricks in the Electric Arc Furnace when Refining with Oxygen. H. Werwach. (*Stahl u. Eisen*, 1952, **72**, Mar. 27, 341–345). The reasons are given for the unsatisfactory performance of rammed tar–dolomite hearths and side-walls of electric arc furnaces producing low-carbon stainless and heat-resistant steels from highly alloyed scrap

with the use of oxygen. The first trials of unstabilized dolomite bricks of German manufacture are reported. These trials led to a new method of lining the whole furnace with binder-free dolomite bricks of improved properties. A special water-free mortar for laying the bricks was developed and tested. In this way it was possible to carry out economically a continuous series of melting operations for the production of difficult steels with a very high furnace availability.—J. P.

The Use of Dolomite Bricks in Open-Hearth and Electric Arc Furnaces. F. Gareis. (*Stahl u. Eisen*, 1952, **72**, Mar. 27, 345–347). Successful trials of a proprietary brand of dolomite bricks in back walls, end stoppings, jambs, and ports of open-hearth furnaces, and in hearths, jambs, and arches of arc furnaces are reported. The new bricks exhibit smaller shrinkage and greater resistance to temperature variations and slag penetration than previous dolomite bricks. A special mortar, resistant to temperatures up to 1200° C. but of unspecified composition, was used for setting the bricks in certain places.—J. P.

Properties of Some Bottom Ramming Materials. M. P. Fedock. (*J. Met.*, 1952, **4**, Mar., 247–249). The author examines and compares the properties of six proprietary materials used in ramming electric furnace bottoms. The materials were selected to emphasize the wide variation in the properties considered, which include bulk density, apparent porosity, cold crushing strength, shrinkage after firing, and rammability.—G. F.

Hot Metal Mixer Linings: Mixer Linings at Columbia-Geneva Steel Div. R. E. Aikens. (*J. Met.*, 1952, **4**, June, 587–588). The author briefly describes tests involving variations in bricks, mortar, and construction of the lining, carried out in an attempt to increase mixer life at the Columbia-Geneva works of the United States Steel Corp. The use of chemically bonded magnesite–chrome bricks in the parts subject to the most severe erosion has increased the service life approximately four times that obtained originally.—G. F.

Hot Metal Mixer Linings: Mixer Linings at Clairton Works. F. R. Smith. (*J. Met.*, 1952, **4**, June, 588). The life of the mixer linings at the Clairton works of the United States Steel Corp. has been considerably prolonged by the use of super-duty bricks with a safety lining of blast-furnace quality brick. Brief details of the construction of the lining and of its performance are given.—G. F.

Abrasion-Resistant Refractory Materials as Applied to Blast-Furnace Operations. F. C. Roe and H. S. Schroeder. (Eastern States Blast Furnace and Coke Oven Assoc.: *Blast Furn. Steel Plant*, 1952, **40**, Apr., 429–435, 442, 458, 460). The use of bonded silicon carbide as a lining for cyclone dust collectors fitted to sintering machines has resulted in a substantial improvement in operation due to a reduction in the amount of wear. Silicon carbide has also been used successfully at elbows and tees where the lining is a target for hot dust-laden gases; circular bricks are available. A fused cast composition of alumina and chrome used for special shapes required in a Pease-Anthony venturi gas scrubber has given very satisfactory results. Coke chutes have also been lined with silicon carbide and the results were very good.—B. G. B.

BLAST-FURNACE PRACTICE AND PRODUCTION OF PIG IRON

The New Blast-Furnace and Ore-Preparation Plant at Société Anonyme John Cockerill, Seraing. J. Miles and T. Thomson. (*Iron Coal Trades Rev.*, 1952, **164**, May 16, 1073–1078). The blast-furnace and ore-preparation plant at the Seraing works of the Soc. An. John Cockerill has recently been modernized. A description of the new installations is given, with details of the crushing, screening, and sintering plant, and of the new blast-furnace and its associated equipment.—G. F.

Blower Selection for Blast-Furnace Operation: Analysis of Records at Two Ironworks. G. A. Flint and J. G. Lee. (*Iron Coal Trades Rev.*, 1952, **164**, June 13, 1301–1306). Factors governing the specification of blowers for blast-furnace duty are considered and the operation over representative periods at two works is analysed. The relation between blower specifications and blast requirements based on this analysis is discussed. The economics of installing a blower larger than needed by current requirements, in order to allow for anticipated future needs, are examined and the conditions which justify this are determined.—G. F.

The Effect of Basic Oxides on Desulphurization in the Blast-Furnace. B. Weilandt, H. Maetz, and W. Oelsen. (*Arch. Eisenhüttenwesen*, 1952, **23**, May–June, 163–172). The ratio of the solubilities of sulphides in the iron melt and slag and the reducing conditions control the degree of desulphurization of iron by molten silicate slags. With increasing reaction time the sulphur content of pig iron decreases and its silicon content increases. Under otherwise constant conditions, the sulphur in the iron is higher the lower the basicity of the metal oxides in the slag. When the lime in a calcium metasilicate or an aluminosilicate slag is progressively replaced by stoichiometric amounts of magnesium, strontium, barium, or sodium oxide, the desulphurizing action is reduced by the magnesia and increased by the other oxides, particularly by sodium oxide. This is explained on the basis of the formation of stable silicates in the slag.—J. P.

Desulphurization in the Blast Furnace: Factors Controlling the Efficiency of the Process. (*Iron Coal Trades Rev.*, 1952, **164**, May 30, 1216–1218, 1215). The efficiency of desulphurization in the blast-furnace is affected by the fact that the furnace conditions do not allow equilibrium values of sulphur distribution between slag and metal to be reached. Factors affecting the approach of these conditions towards equilibrium are considered, and recent research is reviewed.—G. F.

Desulphurizing Iron Outside the Blast Furnace: Review of Some Recent Developments. (*Iron Coal Trades Rev.*, 1952, **164**, Mar. 7, 531–534). In this review, brief reference is first made to the relatively expensive soda-ash treatment. Descriptions are then given of the Kalling process, in which iron is treated with pulverized lime in a cylindrical rotary furnace, and of the use of agglomerated lime balls and lime-coated coke. Finally reference is made to calcium carbide treatment, which is highly effective but more expensive than soda-ash treatment.—G. F.

Effect of Oxygen-Enriched Blast upon Temperature and Size of Coke Combustion Zone (In Blast-Furnace). P. A. Shchukin and L. V. Pegushina. (*Izvestiya Akademii Nauk, Otdelenie Tekhnicheskikh Nauk*, 1950, No. 7, pp. 1034–1039). The effect of oxygen enrichment of the blast on the temperature and size of coke combustion zone was investigated in a 300-mm. dia. shaft furnace. Oxygen enrichment increases the rate and temperature of coke combustion and causes a contraction of the combustion zone. The rate of combustion of coke in an oxygen-enriched blast increases mainly in the vicinity of the tuyeres.—V. G.

Review of High Top Pressure Operation. J. R. Barnes. (Eastern States Blast Furnace and Coke Oven Assoc.: *Blast Furn. Steel Plant*, 1952, **40**, Apr., 407–409, 415). Bell and hopper erosion has been reduced by equalizing the pressure between the bells after each small bell dump. Hard surfacing is not being applied to bells and hoppers at present. Operating data from several American furnaces are considered. The installation of a new and larger blower at one furnace has shown how iron tonnage is a function of wind blown. An increase in the life of the lining is reported for several furnaces now operating with high top pressure.—B. G. G.

Operation of the Sheffield Steel Corporation's Blast-Furnace, Houston, Texas. F. C. Davies. (*Amer. Inst. Min. Met. Eng., Blast Furn., Coke Oven, Raw Mat. Conf.*, 1951, Apr., 228–243). An account is given of the coking coal and iron ore supplies and the blast-furnace and open-hearth practice at the above Corporation's works where, in 1947, there was one 22-ft. dia. hearth blast-furnace and six open-hearth furnaces.—R. A. R.

Results Obtained from Use of High-Magnesia Slags. L. F. Moore. (*Amer. Inst. Min. Met. Eng., Blast Furn., Coke Oven, Raw Mat. Conf.*, 1951, Apr. 187–199). A comparison is made of operating data of two similar blast-furnaces of the Geneva Steel Co., Salt Lake City, one using limestone as flux and the other using dolomite. The effect of a high-magnesia slag on operation is outlined, but no marked difference was observed in production or iron quality.—T. E. D.

Equilibrium between Blast-Furnace Metal and Slag as Determined by Remelting. E. W. Filer and L. S. Darken. (*Trans. Amer. Inst. Min. Met. Eng.: J. Met.*, 1952, **4**, Mar., 253–257). The authors have examined the equilibrium between blast-furnace metal and slag, by taking samples simultaneously from the same furnace and remelting them in a graphite crucible in a CO atmosphere. The results show that complete equilibrium is by no means reached in the blast-furnace, and that both desulphurization and manganese

recovery would be substantially improved if the conditions in the furnace more closely approached equilibrium.—G. F.

Relative Reducibility of Ores and Agglomerated Materials in Various Stages of Oxidation. C. Ludwig. (*Amer. Inst. Min. Met. Eng., Blast Furn., Coke Oven, Raw Mat. Conf.*, 1951, Apr., 173–186). The reducibility of lump ores and extruded pieces of similar composition is compared, and some earlier apparent anomalies are explained. When similar agglomerates of Fe_2O_3 and Fe_3O_4 were tested at 900° C., the latter was reduced more easily, but at 800° C. the reverse was true.

Hydrogen as an Additional Reducing Agent to Coke in the Blast Furnace. J. L. Boyle. (*Blast Furn. Steel Plant*, 1952, **40**, May, 535–537). The author suggests that hydrogen, either mixed with hydrocarbons as in coke-oven gas or alone, could be used as an additional reducing agent to coke in the blast-furnace, in which it would reduce the consumption of coke.—B. G. B.

Hydrogen Reduction of Iron Ore. C. A. Scharschu. (*J. Met.*, 1952, **4**, Mar., 250–251). A brief description is given of pilot-plant trials of the direct reduction of iron ore by hydrogen, carried out at the works of the Allegheny Ludlum Steel Corp. Although the trials showed that satisfactory results can be obtained by hydrogen reduction, it is concluded that further information must be obtained from large-scale operations before the process can be fully evaluated.—G. F.

Performance of the Carbon Hearth. H. M. Kraner and K. A. Baab. (*Amer. Inst. Min. Met. Eng., Blast Furn., Coke Oven, Raw Mat. Conf.*, 1951, Apr., 165–173). The number of blast-furnaces with carbon hearths installed between 1945 and 1950, and the types of installation are given. A detailed summary of performance data for carbon in blast-furnaces is also included, and confidence is expressed in its use.—T. E. D.

Carbon Hearths for Blast Furnaces: Some Thoughts and Calculations. E. W. Voice. (*Iron Coal Trades Rev.*, 1952, **164**, June 6, 1265–1267). Problems of heat loss in blast-furnace carbon hearths arising from the high thermal conductivity of carbon are discussed, and the calculation of temperature distribution throughout the hearth is considered. Thermal control of the hearth bottom by air-cooling immediately below the hearth is suggested, and the lines upon which model experiments should be conducted are outlined.—G. F.

Statistical Analysis Becomes of Age—An Example of Analysis of Blast-Furnace Production Data. D. S. Leckie. (*Amer. Inst. Min. Met. Eng., Blast Furn., Coke Oven, Raw Mat. Conf.*, 1951, Apr., 153–164). A general account is given of the selection and tabulation of blast-furnace data. The punched-card system of filing and computation is mentioned.—T. E. D.

Blowing-In Blast-Furnaces under Particularly Difficult Conditions. P. Thierry. (*Mét. Constr. Méc.*, 1952, **84**, Mar., 153–157; Apr., 217–219). An account is given of the difficulties encountered in restarting a French ironworks which had been suddenly abandoned in May, 1940, when overrun by the enemy.—R. A. R.

Procedure Used in Quenching and Digging Out a Blast Furnace and Some Results. K. C. McCutcheon, W. E. Marshall, and H. C. Barnes. (*Amer. Inst. Min. Met. Eng., Blast Furn., Coke Oven, Raw Mat. Conf.*, 1951, Apr., 200–227). An experimental run using a burden of up to 100% pellets made from Minnesota taconite was performed in a small (3-ft. dia. hearth) furnace. At the conclusion of the run a number of marked heavy screen-wire containers and marked slugs were charged in each of the last 25 rounds. The furnace was quenched using nitrogen gas. The burden was then dug out in layers, each of which was photographed and sampled. Illustrations are shown, the results are discussed, and an overall mechanism of smelting in the blast-furnace is outlined.—T. E. D.

PRODUCTION OF STEEL

Steel in South Africa: Krupp-Renn Plant and Steelworks to be Erected by Dominion Iron and Steel Corporation. (*Iron Steel*, 1952, **25**, Mar., 93–94, 101). Brief details are given of a new works to be erected by the Dominion Iron and Steel Corp. in Eastern Transvaal. The works will consist of a Krupp-Renn plant for the direct reduction of certain types of ores, a Bessemer plant, and steel rolling mills. The projected annual outputs of pig iron and rolled steel are 70,000 and 48,000 tons respectively.—G. F.

A Modern Brazilian Integrated Iron and Steel Works: Equipment and Practice at Companhia Siderurgica Belgo-Mineira. (*Iron Coal Trades Rev.*, 1952, **164**, May 16, 1093–1095). A description is given of the integrated iron and steel works of the Companhia Siderurgica Belgo-Mineira, situated at Sabara and Moulevade in the most important mineral zone in Brazil. Brief details are given of the ore deposits, sintering plant, blast-furnaces, steel plant, rolling mills, wire-drawing department, and tube-making plant.—G. F.

Casting Steel Ingots: Development of Casting Techniques and Trials with Basic Refractories in Ladles. (*Iron Coal Trades Rev.*, 1952, **164**, May 30, 1219–1222). This article summarizes part of the proceedings of the 38th B.I.S.R.A. Steelmaking Conference. The topics discussed were: Experiments on the influence of the shape and constitution of ingot feeder-heads on ingot yield; trials of basic refractories in open-hearth ladles; and the relation between teeming speed and quality of structural steel ingots.—G. F.

Prerefining High-Silicon Basic-Bessemer Iron with Pure Oxygen. P. Leroy. (*Rev. Mét.*, 1952, **49**, Mar., 231–241; Apr., 299–320). The advantages of acid working are cited, the main drawback being too high a silicon content or two low a temperature. To overcome this, trials with oxygen blown into the ladle at the slag-metal interface were performed, and the variations in iron composition and temperature studied. Thermal and material balances are given for the process and its effect on Bessemer operation is discussed. With 4–5 cu. m. of oxygen per ton of iron, the silicon loss was 0·22–0·31%; the manganese loss 0·07–0·08%; the iron loss 1·2%; and the temperature rise 90° C.—A. G.

Liquid Charging: Should it be More Widely Adopted ? J. O. Griggs. (*Iron Steel*, 1952, **25**, Apr., 131–133). The author discusses the duplexing and triplexing methods of steelmaking and compares them with normal open-hearth practice, indicating the possible savings in materials, labour, and maintenance. The use of tilting furnaces for these processes is advocated. A method of top-charging open-hearth furnaces with scrap, using the overhead crane and drop-bottom boxes, is proposed and its advantages over conventional charging methods are enumerated.—G. F.

Effect on Production Rates of Foreign Ores as Open-Hearth Charge Ore: Liberian Ore. R. P. Carpenter. (*J. Met.*, 1952, **4**, June, 592–593). A description is given of the use of Liberian ore as an open-hearth charge ore at the Cleveland works of the Republic Steel Corp. Liberian ore appears to approach the ideal charge and feed ore more closely than any other used in the industry today.—G. F.

Effect on Production Rates of Foreign Ores as Open-Hearth Charge Ore: Moroccan Ore. C. C. Benton. (*J. Met.*, 1952, **4**, June, 593). The author briefly describes trials of Moroccan ore as an open-hearth charge ore at the Fairfield works of the United States Steel Corp. This ore can be used in combination with low-sulphur fuels with advantage to production.—G. F.

Free-Cutting Steel: Manufacturing Developments during the Last Thirty Years. J. A. Anderson. (*Brit. Steelmaker*, 1952, **18**, June, 302–305, 310). The author discusses: (1) Developments in the manufacture of free-cutting steel in the period 1920–1940, with special reference to machinability. (2) The period 1946–1950, giving particular attention to a process for the manufacture of free-cutting steel with a 100% scrap charge, re-using the same manganese slag, and reducing the lime feed to negligible proportions. Details of the great savings in lime and ferro-manganese are given.—G. F.

Some Factors Affecting the Operation and Performance of Open-Hearth Furnaces. D. F. Marshall. (*J. Iron Steel Inst.*, 1952, **172**, Nov., 315–326). [This issue].

Conservation and the Open Hearth. F. F. Franklin. (National Open Hearth Committee: *Blast Furn. Steel Plant*, 1952, **40**, May, 522–524). A review of the availability of alloying elements in America is given. Manganese ores should not be used in the blast-furnace. To conserve manganese it should be added after the silicon and aluminium and not used as a deoxidizer. Ferromanganese should be added to the ladle whenever possible. Methods of reducing nickel and chromium consumption in alloy steel production are also given.—B. G. B.

Influence of Manufacturing Conditions in the Steel Plant on the Quality of Forging Ingots. W. Heischkeil. (*Stahl u. Eisen*, 1952, **72**, June 5, 663–668). A review of the numerous defects in heavy forging ingots indicates the importance of steelworks practice on quality. The extensive literature is critically summarized and precautions are indicated dealing mainly with the contradictory requirements of low and high teeming speeds and temperatures and their influence on

premature freezing and inclusions. The effect of aluminium content on transcrystallization is clarified.—J. P.

Handling in Steelworks and Rolling Mills. C. Rondelle. (*Rev. Univ. Min.*, 1952, 9th series, **8**, May, 139–156). Often 30% of the power used in steelworks is connected with mechanical handling. A detailed examination is made of the movement of materials in the production of basic-Bessemer steel and blooming-mill operation.—B. G. B.

Reaction between Sulphur Dioxide and Molten Iron: Its Influence in Steelmaking. P. T. Carter and S. Tahir. (*Iron Coal Trades Rev.*, 1952, **164**, Apr. 25, 917–919). The results of experimental work carried out in the study of the reaction between SO_2 and molten iron under different conditions are described, and the importance of this reaction in the steel-making process is discussed. A theory is suggested to explain the mechanism of sulphur pick-up in the metal. The authors emphasize the need for further study of the reactions between the furnace gases and the charge in the O.H. furnace.—G. F.

Studies in the Deoxidation of Iron: Deoxidation by Silicon. E. Ll. Evans and H. A. Sloman. (*J. Iron Steel Inst.*, 1952, **172**, Nov., 296–300). [This issue].

The Hot-Blast Cupola and Its Metallurgical Applicability. K. Guthmann. (*Stahl u. Eisen*, 1952, 72, 8, 545–556). The method of operation and production possibilities of the hot-blast cupola are discussed. The possibility is indicated of making a very good metal charge for steelworks by incorporating a hot-blast cupola plant. A synthetic pig iron for liquid charges in the basic-Bessemer converter and the open-hearth and electric-arc furnaces can be made by melting down up to 100% steel scrap with, if necessary, additions of home ore, iron-rich sintered ore, or acid blast-furnace metal. The behaviour of carbon, silicon, manganese, and sulphur in acid and basic lined cupolas is described. A special advantage of the cupola is that an iron of any desired composition can be produced, *e.g.*, a low-carbon iron (down to 1·5% C) tapped at 1500–1600° C. can be produced from steel scrap and pig iron, casting scrap, etc. Some modern plants are illustrated and described.—J. P.

Influence of Hot-Blast Cupola Operation on the Economy of Open-Hearth Steel Ingot Production. G. Prieur. (*Stahl u. Eisen*, 1952, **72**, May 8, 556–561). The use of hot-blast cupola iron as hot-metal charge for open-hearth furnaces alters charge and operating costs, production rate, and total output. The technological and cost factors responsible, the way in which they affect the economics of ingot steel production, and planning the introduction of a hot-blast cupola between the scrap yard and the open-hearth furnace are indicated. In general, though the economy of the cupola is assured, the savings will depend on prevailing circumstances and each case should be examined on its merits.—J. P.

Recent Experience with Basic and Acid Electric Steel. L. W. Sanders. (*Inst. British Found.*, June, 1952, *Paper No.* 1041). **Production of Steel in Acid and Basic Electric Furnaces: Some Recent Experiences.** L. W. Sanders. (*Iron Coal Trades Rev.*, 1952, **164**, June 13, 1315–1320). Particulars of a new plant installed at the works of Lake and Elliot Ltd. for the production of acid and basic electric steel for castings are given, and details of the linings, the melting processes, and degree of uniformity and reproducibility of steels are presented. The acid furnace has replaced the Tropenas converter plant, and the author describes his experiences gained in the operation of both the new and old processes, indicating the reason for the change.—G. F.

Efficient Utilization of Titanium Alloys in Stainless Steel Production. F. St. Vincent and R. W. Rebholz. (*J. Met.*, 1952, **4**, Mar., 245–246). The production of titanium-stabilized stainless steels is discussed and the precautions necessary to obtain the most efficient utilization of the titanium are considered. Factors affecting the titanium recovery, and in particular the conditions under which its stable oxides and nitrides are formed, are also discussed.—G. F.

Solidification and Segregation in Killed Steel Ingots. E. A. Loria. (*Blast Furn. Steel Plant*, 1952, **40**, Apr., 410–415). A review of the mechanism of the solidification of steel ingots is made and a number of proposed theories are discussed. The major portion of the segregation occurring is produced by vertical solidification.—B. G. B.

Homogenization and Dehomogenization of Steels. L. Colombier. (*Métaux–Corrosion–Indust.*, 1950, **25**, Dec., 295–307). The segregation of impurities during the solidification of steels and the homogenization by subsequent heat-treatment are described with the help of phase diagrams. Care must be taken not to give excessive heat-treatment which causes dehomogenization; theories concerning this are discussed. The effect of dissolved hydrogen on crystalline structure and several theories to explain the effects are reviewed.—B. G. B.

FOUNDRY PRACTICE

Moulding Sand in the Foundry. E. Leonhardt. (*Giesserei*, 1952, **39**, May 29, 252–255). The author strongly recommends the use of sand tests even in small foundries, since bad moulding sand is usually the cause of casting rejection. He discusses various sands, including synthetic and semi-synthetic sands, and their properties.—J. G. W.

Sand-Mixing Machines. W. Gesell. (*Giesserei*, 1952, **39**, Apr. 17, 186–189). The purpose of sand mixing is explained and the various types of centrifuges and mixers are described and illustrated.—J. G. W.

Heat Flow in Moist Sand. V. Paschkis. (*Amer. Found. Soc. Preprint*, 1952, No. 4). Computation shows that thin castings will freeze in considerably shorter time under the influence of moisture, whereas the influence of moisture in the case of thick castings is limited to an increase in the solidification rate of the very first layer to freeze; the total solidification time in the case of thick castings is not affected.—E. T. L.

Standards for Coal Dust in Moulding Sands and Instructions for Its Analysis. (*Gjuteriet*, 1952, **42**, Jan., 13–14). [In Swedish]. The following Swedish standards have been proposed: Moisture content 5% max., ash 10% max., volatile matter 28% min., the last two as percentages of the dried sample. The moisture to be determined by drying the sample at 105–110° C. to constant weight; ash by a combined flame and stove process at 1000° C. or by burning over a flame in oxygen; and volatile matter by heating in an electric furnace for 7 min. at 950 ± 20° C.—G. G. K.

Comparison between American, German and Swedish Specimens for Sand Testing. M. Itzel. (*Gjuteriet*, 1952, **42**, May, 83–84). [In Swedish]. Different standard sample sizes have been adopted in many countries for testing the tensile strength of moulding sands. Test data are given to establish the extent of variations in results due to this lack of uniformity. Compression and bending test results were identical in the case of the Swedish and German specimens, whilst the American specimen exhibited better permeability than the others, due perhaps to variations in ramming methods.

Load-Deformation Curves on Dried Sand Specimens and Their Relationship to Expansion Defects. R. G. Godding. (*British Cast Iron Research Assoc. J. Res. Development*, 1951, **4**, Dec., 202–208). With mixtures containing sand and clay only, strain is almost proportional to stress until, at maximum stress, complete collapse of the test piece and a very rapid fall in stress occurs. Maximum stress is raised with increasing moisture content but the strain at maximum stress is unaffected. Maximum stress is considerably raised and strain is slightly increased with increase in clay content. Scabbing is likely to occur when the strain at maximum stress is low and the test piece collapses rapidly on reaching maximum stress. Scabbing is unlikely to occur when the strain is high and stress falls slowly with increasing strain after maximum stress is reached. The results obtained by addition of wood flour are also discussed.—B. G. B.

Machine Makes Cores Faster, Cheaper, Better. H. Chase. (*Iron Age*, 1952, **169**, Mar. 27, 93–97). A special indexing machine developed for sand core making is being used at the Buick Motor Division, thus eliminating all handling operations except for placing of wires when needed. Basically, the machine is a five-station indexing table with a modified Geneva motion to avoid shocks in accelerating and decelerating.—A. M. F.

Core-Blower Application and Operation. G. W. Fearfield. (*Proc. Inst. Brit. Foundrymen*, 1951, **44**, B31–B39). Machine moulding necessitates core blowing, particularly with small cores. General conditions governing the choice of machine and air needed, are given. The sand to use, core-box materials and design, the position of blowing holes, and the theory of venting are considered.—E. T. L.

Economical Working with the Sandslinger. H. Kleinschmidt. (*Giesserei*, 1952, **39**, May 29, 269–270). Various types of sandslinger are described.—J. G. W.

Proposals for Testing Core Sands, Core Binders, and Cores. F. Roll. (*Giesserei*, 1952, **39**, May 1, 203–207). The proposals

cover methods of taking samples, chemical tests, ash determination, physical tests, and microscopical examination.

High-Frequency Core-Drying by Means of Capacitative Heating. H. Derlon and C. Rauh. (*Giesserei*, 1952, **39**, Apr. 17, 179–181). High-frequency drying offers attractive possibilities where hardening of cores is achieved through drying. Its advantages are illustrated by results of tests which show greater drying speed compared with infra-red heating.

Gating in Moulding for Malleable Cast Iron. W. Dahlbüdding. (*Giesserei*, 1952, **39**, June 12, 293–295). The principles of proper design of gates and risers for malleable iron castings are explained and illustrated.—J. G. W.

Practical Example of Moulding Technique for a Steel Casting. H. Bellscheidt. (*Giesserei*, 1952, **39**, May 29, 262–264). The design of the pattern and preparation of a mould for a cast steel cover for a ball mill is described in detail.—J. G. W.

Chilled-Roll Manufacture. K. H. Wright. (*Proc. Inst. Brit. Foundrymen*, 1951, **44**, B13–B30). After considering briefly the history and the requirements of chilled rolls, the author describes the structure in terms of hardness and composition. The production technique, including testing, is discussed.

Intricate Castings from a Durable Loam Mould. J. Currie. (*Proc. Inst. Brit. Foundrymen*, 1951, **44**, B50–B62). The author describes in detail the making of a large evaporator-body casting.—E. T. L.

Loam and Dry-Sand Moulding in the Jobbing Foundry. D. Redfern. (*Proc. Inst. Brit. Foundrymen*, 1951, **44**, B1–B12). The author considers the making of a cylinder block casting and a large water-box. The procedure is illustrated with 27 photographs.—E. T. L.

Loam Moulding of Pump Castings and Impellers. E. Clipson. (*Proc. Inst. Brit. Foundrymen*, 1951, **44**, B74–B84). This is a detailed description of the making of a turbine impeller of the double entry, shrouded pattern type, and also a volute section of the 102-in. centrifugal pump.—E. T. L.

Straight Cast-Iron Pipes. J. L. Handley. (*Proc. Inst. Brit. Foundrymen*, 1951, **44**, B124–B129). This paper describes the methods of making soil and vent pipes.—E. T. L.

Mild Steel Chaplets. (*British Cast Iron Research Assoc. J. Res. Development*, 1952, **4**, Apr., 336–337). A survey to determine the best metallic coating on mild-steel chaplets has been carried out. Provided that the coating is thin and uniform, either dip or electro-tinning is suitable, but the lead content is kept to a minimum. Copper may also be applied but, owing to its red colour, it is difficult to differentiate between an intact coating and rust. Coatings of zinc, lead, or cadmium should not be used.—B. G. B.

Investment Casting. (*Machinery*, 1952, **80**, Feb. 21, 311–319). Equipment, methods, and techniques employed by the B.S.A. Co., Ltd., Birmingham, are reviewed.—E. C. S.

Experimental Investment Castings. D. F. B. Tedds. (Inst. Brit. Foundrymen: *Machinist*, 1952, **96**, Apr. 5, 499–504). **Production Problems of the Investment Casting Process.** (*Machinist*, 1952, **96**, Mar. 22, 419–425). It illustrates the possibilities and limitations of the investment casting process by referring to work at the precision foundry of the Bristol Aeroplane Co., Ltd.—E. C. S.

Investment Casting Quality Tied to Gating, Mold Turbulence. R. L. Wood and D. von Ludwig. (*Iron Age*, 1952, **169**, Feb. 21, 93–96). Arwood Precision Casting Co. have studied the quality of investment castings and find that the flask temperature does not affect hardenability or tensile strength of 4140 and other alloy steels. The casting quality is tied to gating designed for minimum turbulence. Proper gating produces the cleanest castings, least scrap, and minimizes inclusions.

Centrifugal Casting. H. Jungbluth. (*Gjuteriet*, 1952, **42**, Jan., 1–10). [In Swedish]. Mathematical and physical laws governing centrifugal casting are examined and equations derived for forces operating when casting in moulds with horizontal and vertical axes of rotation. The relationship between speed of rotation and internal diameter of the casting is discussed. Developments in casting iron and steel pipes by the Billaud and Projohn methods are reviewed and comparisons made of the tensile strengths of cast-iron cylinder liners and non-ferrous alloys when cast centrifugally or in sand.—G. G. K.

Basic Theory of Heat Transfer in Moulds. F. L. Daws. (Birmingham University Conf. on Solidification of Castings: *Metal Ind.*, 1952, **80**, May 16, 406, 407). The author reviews very briefly the many theoretical treatments of the rate of heat transfer in moulds. (27 references).—P. M. C.

Application of Heat Transfer Theory to Ingot Casting. J. J. Pick. (Birmingham University Conf. on Solidification of Castings: *Metal Ind.*, 1952, **80**, May 16, 407–409). The author first discusses the gap formation between ingot surface and mould wall, and then deals with the effect of mould design. Attempts at a theoretical prediction of the shape of the liquid–solid interface in the ingot are referred to.—P. M. C.

Application of Heat Flow Theory to Sand Castings. G. Martin. (Birmingham University Conf. on Solidification of Castings: *Metal Ind.*, 1952, **80**, May 23, 427–429). The author discusses the thermal properties of casting sands and metal, and emphasizes that theoretical prediction of heat flow and solidification rate is still in its infancy. Risering problems are also referred to. (22 references).—P. M. C.

Core Removal and Cleaning of Castings by the Wet Process. W. Russell. (*British Cast Iron Research Assoc. J. Res. Development*, 1952, **4**, Feb., 263–271). Modern developments in the wet-cleaning (hydro-blast process) of medium and large castings have led to a reduction in water consumption by using small volumes of water at high pressures (2000 lb./sq. in.); the stream being directed at high velocity on to the casting. Details of the equipment available and operation of the plant, including reclamation of sand and water, are given.—B. G. B.

HEAT-TREATMENT AND HEAT-TREATMENT FURNACES

A New Gear Production and Heat Treatment Installation. (*Machinery*, 1952, **80**, June 5, 998–1002). **A Gear Cutting and Heat Treatment Plant.** (*Engineer*, 1952, **193**, May 23, 706–707). **Machining and Heat Treatment of Gears.** (*Engineering*, 1952, **173**, May 23, 659–660). **Heat-Treatment of Steel Gears.** (*Metal Treatment and Drop Forg.*, 1952, **19**, June, 251–254: *Metallurgia*, 1952, **45**, June, 297–298). This article describes the new shops at Rochester for Blaw Knox, Ltd. The shops comprise cutting and heat-treatment departments. The cutting section is equipped for the manufacture of internal and external spur wheels, helical gears, worms, and straight and curved bevels. The heat-treatment shop is equipped for gas carburizing, tempering, refining, normalizing, and hardening.

Protective Gases for the Heat-Treatment of Steels. E. Kunze. (*Stahl u. Eisen*, 1952, **72**, May 8, 561–569: Iron Steel Inst. Translation Series, 1952, No. 453). The literature on the theory and practice of the use of protective gases for heat-treatment is reviewed and the oxidation, carburization, and water-gas equilibria, are discussed. The protective gases recommended in the literature have been developed experimentally. Since it would be advantageous to limit the number of components of protective atmospheres, a ternary carbon–oxygen–hydrogen diagram has been devised, which shows the action of any gas mixture derived from these three elements and from which the variation of composition, by such methods as cracking, combustion, drying, carburizing, CO_2 removal, water addition, for any given purpose may be calculated. According to the type of steel to be annealed, the gas mixture must be carburizing, reducing, or inert, and the need for correct composition and purity increases with the surface quality of the steel desired. The search for the cheapest way to produce a desired mixture from given raw materials will be facilitated by the use of the diagram.—J. P.

Files and Tools: Advantages of Heat Treatment in Salt Baths. D. Thomas. (*Iron Steel*, 1952, **25**, Mar., 85–88). The author deals with the general advantages of salt-bath heat-treatment of files and tools, and discusses the more recent developments in salts produced for specific purposes. The technique of salt-bath heat-treatment and the appropriate equipment are described, and the particular advantages of each process are outlined.—G. F.

Heat Treatment for Rope Wires. R. Saxton. (*Min. Mag.* 1952, **86**, Mar., 146–148). A description is given of the wir patenting process which is a form of continuous annealin and toughening which is followed by cleaning and drawing. Tensile, torsion, flexion, looping, and wrapping tests on wire are also described.—H. F.

FORGING, STAMPING, DRAWING, AND PRESSING

Steels for Large Forgings. S. Ammareller and P. Grün. (*Stahl u. Eisen*, 1952, **72**, June 5, 653–662). The suitability of acid and basic open-hearth and electric furnace steels for various types of forging is discussed. In the working of the ingots there should be a two-and-a-half to three-fold reduction of cross-section in order to ensure good longitudinal and transverse mechanical properties. Testing may be conducted on test bars machined from the finished product and by means of non-destructive ultrasonic equipment. The advisability of a minimum impact strength for mildly stressed components is questioned. The composition of steels and preferred heat-treatments for large forgings in Germany and elsewhere are described.—J. P.

The Deformability of Slag Inclusions in Steel and its Significance in the Assessment of Forgings. E. Scheil and R. Schnell. (*Stahl u. Eisen*, 1952, **72**, June 5, 683–687: Iron Steel Inst., 1952, Translation Series No. 454). Sulphides in steel are surprisingly easily deformed if they are totally enclosed, whereas at a free surface they are brittle. The deformability is practically independent of temperature in the range − 80° to + 1250° C. Oxide inclusions are so hard at low temperatures that they shatter. At higher temperatures they soften. Their deformability then approaches that of steel. The upper and lower limits of this temperature range depend on the metallurgical quality of the steel and they appear to be a useful and comparatively easily determined criterion of this quality. By measuring the deformation of sulphide inclusions in forgings and rolled products, it is possible to determine the degree of uniformity of deformation of the whole material. The deformation of the oxide inclusions gives information about the temperature of forging or rolling.—J. P.

Forging Manipulators. W. Knackstedt. (*Stahl u. Eisen*, 1952, **72**, June 5, 668–675). The construction and installation of forging manipulators developed in the U.S.A. are reviewed and some American forging plants are described with special reference to the movement of materials and working times.—J. P.

Press Forging Dies: Some Practical Considerations. E. W. Mace. (*Iron Steel*, 1952, **25**, Apr., 135–137). The author discusses the design of the dies fitted to the bolster of the forging press for the manufacture of steel forgings, and outlines the practical considerations which have to be taken into account.—G. F.

New Die-Forging Hammer at the Gerlafingen Forge. A. Brossi. (*Von Roll Werkzeitung*, 1952, **23**, May, 84–86). This new counterblow hammer produces die forgings up to 800 kg. in weight. The upper of the two tups, which approach each other at equal velocity, is driven by steam or compressed air, and the lower one is drawn upwards by steel bands. A blow of about 40 ton-metres is obtained.—R. A. R.

A New Forging Machine. A. Stodt. (*Stahl u. Eisen*, 1952, **72**, June 5, 704–706). A new forging machine of Swiss manufacture is described. The forging is performed by three equally spaced radial hammers, driven by cam-shafts, which each deliver 500 blows/min. The initial billet is held vertically in a spring-loaded, compressed-air-operated head which moves it up and down between the hammers. The amount of forging is controlled by the settings of the cams and followers, and these and the movement of the head are in turn controlled by a template, so that between inserting the work piece and removing it, the machine functions automatically. It can produce forgings between 20 and 80 mm. in dia. from 250 to 1000 mm. long.—J. P.

Making of a 70 Ton Forged Steel Chemical Converter-Tower. R. A. MacGregor. (*Metal Market Review*, 1950, Feb., 107–111). To illustrate his belief that India should instal plant capable of making large forgings the author describes the making of a 40-ft. converter tower from the production of the initial 140-ton ingot to the final machining.—B. G. B.

Fencing of Hydraulic Presses. (Ministry of Labour and National Service, Report of the Joint Standing Committee on Safety in the Use of Power Presses, 1952: H.M. Stationery Office). **Fencing of Press Brakes.** (*Ministry of Labour and National Service, Report of the Joint Standing Committee on Safety in the Use of Power Presses*, 1952: H.M.S.O.).

ROLLING-MILL PRACTICE

A Heavy Duty Slip Regulator for Steel Mill Service. W. Schaelchlin and G. E. Mathias. (*Trans. Amer. Inst. Elect. Eng.*, 1951, **70**, Part II, 1769–1773). The design of a new electrolytic slip regulator is described. High efficiency is obtained from the sodium carbonate electrolyte and the heat-exchanger system. Results are given of a 6000-h.p. mill motor showing that with regulation, power peaks of 130% become negligible.—H. D. W.

Draft Dials: Their Graduation in Rolling Mills. J. H. Mort. (*Iron Steel*, 1952, **25**, Mar., 79–83, 97; Apr., 123–126, 128). The author considers the graduation of draft dials on rolling mills engaged in the rolling of flats and in which several passes are given on one stand, discussing in particular roughing mills of the 2-high and mechanized 3-high types. Formulæ are presented for constructing draft charts and for graduating the dials, and their practical application is described. Automatic presetting apparatus is also briefly discussed.—G. F.

1,100 mm. Two-High Blooming and Slabbing Mill. F. Münker. (*Demag News*, 1952, No. 127, 1–4). The mill, which is described in detail, is capable of rolling 1,000,000 tons of ingot steel per annum and takes ingots weighing from 4–6 tons and slab ingots from 10–12 tons. The electrically driven manipulators can handle slabs of up to 4 ft. 5 in. wide.

Reconstruction at Karabuk: Contract for Davy-United. (*Iron Steel*, 1952, **25**, Apr., 140). A brief description is given of the proposed large-scale extension and modification of the existing 28-in. blooming and section mill plant at the Karabuk Iron and Steel Works in Turkey. The work will be carried out by Davy and United Engineering Co. Ltd.—G. F.

Manufacture of Wheels, Tyres and Axles in India. P. H. Kutar. (*Metal Market Review*, 1950, Feb., 119–122). An account is given of the manufacture of wheels, tyres, and axles for locomotives and rolling stock. Production, started during the war, is now such as to make India self-sufficient.

The New Appearance of Ougrée-Marihaye. (*Rev. Univ. Min.*, 1952, 9th series, **8**, May, Monographies Industrielles). A new rolling mill with a capacity of 300 tons of heavy plates and 800 tons of wide strip per 8-hr. shift is described.—R. A. R.

Application of the Theories of Plasticity to the Working of Metals. The Rolling of Thin Strip. J. Lerebours-Pigeonniere. (*Métaux–Corrosion–Indust.*, 1952, **27**, Mar., 119–134). The influence of temperature on the resistance of the metal to deformation is discussed and two cases are considered: (1) Where viscous and plastic forces are involved, and (2) purely plastic deformation. The mechanism of the rolling of steel strip is then treated mathematically and a number of theories which have been proposed are discussed.—B. G. B.

The Speed-Dependent Variables in Cold-Strip Rolling. R. B. Sims and D. F. Arthur. (*J. Iron Steel Inst.*, 1952, **172**, Nov., 285–295). [This issue].

Universal Tube and Section Mill. (*Engineering*, 1952, **173**, May 30, 669–672). A detailed description is given of the Henderson universal tube and section mill designed by Tube Making Machines Ltd., Blaenrhondda, Glam. The mill produces not only welded tube in a variety of materials and cross-sections, but also a wide range of cold-rolled sections, all with a single pass through the machine. Other operations, such as piercing, embossing or indenting, can also be done by use of ancillary equipment.—M. D. J. B.

MACHINERY FOR IRON AND STEEL PLANT

Storage of Steel: New Stockyard for Steel, Peech and Tozer. (*Iron Steel*, 1952, **25**, May, 173–174). This article gives brief details of the design and construction of the new stockyard at the Steel, Peech and Tozer works in Sheffield. Three of the four 10-ton cranes received special treatment before fabrication in order that paint and maintenance costs could be studied.—G. F.

Mineral Ore Production: Use of Battery Electric Vehicles at Scunthorpe. (*Iron Steel*, 1952, **25**, Apr., 139–140). The design and use of two battery-operated electric trucks at the Dragonby Mine, Scunthorpe, is described. The vehicles are a 30-cwt. explosives truck and a 2-ton scaling car.—G. F.

Heenan & Froude Jubilee Celebrations: Manufacture of Plant for Iron, Steel, and Coal Industries and Specialised Engineering. (*Iron Coal Trades Rev.*, 1952, **164**, June 27, 1429–1432). A review is given of the achievements and range of products of the Heenan and Froude group of companies, which is celebrating this year its golden jubilee in the production of plant for the iron, coal, and steel industries.—G. F.

Engineering Aspects of Open-Hearth Steel Plants: Consideration of Principal Items of Equipment to Facilitate Production. J. A. Kilby. (*Iron Coal Trades Rev.*, 1952, **164**, May 23, 1135–1141). The author discusses the design and layout of

modern open-hearth melting shops, and describes some of the improved equipment which has helped to increase production. This includes scrap-baling presses, modern cranes, and charging machines. Improvements in furnace design are also indicated.—G. F.

Handling in the Heavy Engineering Industry. E. Dorlet. (*Rev. Univ. Min.*, 1952, 9th series, **8**, May, 156–186). Several different types of handling equipment available are reviewed in detail. The use of fork-lift trucks is described.—B. G. B.

A Recent Development in Ingot Chariot Control. A. W. Leadbeater and R. B. Jamieson. (*J. Iron Steel Inst.*, 1952, **172**, Nov., 264–267). [This issue].

Tyre Billet Slicer. (*Railway Gazette*, 1952, **96**, June 27, 713–714). A machine, designed by Craven Bros. (Manchester) Ltd., is described which is capable of slicing steel ingots up to 24 in. in dia. and 9 ft. long into discs for the manufacture of railway tyres. It is driven by a 100-h.p. motor and uses nine pairs of parting tools.—R. A. R.

WELDING AND FLAME-CUTTING

Weldable Low-Alloy Steels for Pressure Vessels. F. Nehl. (*Stahl u. Eisen*, 1951, **71**, Dec. 20, 1443–1444: *Welding J.*, 1952, **31**, Apr., 231s–232s). As the equipment for producing rolled high-pressure drums in Germany has been destroyed or dismantled, fabrication is now carried out by welding. Mechanical properties and suitability of Cu–Ni–Mo steel for welded pressure vessels are discussed.—U. E.

Fabricating Modern Pressure Vessels. (*Welding Metal Fab.*, 1952, **20**, Apr., 120–129). A review is given of the latest methods in the production of pressure vessels and allied equipment for the oil and chemical industries. A survey of the new fusion welding shop at G. A. Harvey and Co. (London), Ltd., describes the plant, layout, and operating techniques.

Fracture Initiation and Propagation in Welded Ship Steels. P. J. Rieppel and C. B. Voldrich. (*Welding J.*, 1952, **31**, Apr., 188s–201s). The initiation and propagation of fractures in welded and Kinzel-type transversely notched bead-bend specimens were investigated. Ship-plate quality steel welded with E6010 electrodes was used. In all welded specimens, the weld metal cracked first and at very low bend angles. Unwelded plate specimens cracked only after a much greater bend angle was reached. There is a tendency for cracks to begin across weld beads that have been plastically strained in the longitudinal direction.—U. E.

Failures in Welded Ships. (*Welding Metal Fab.*, 1952, **20**, Apr., 130–134). A review is given of the failures in welded ships built between 1939 and 1945. It was found that the fractures which developed in a sudden brittle manner, quite uncharacteristic of the ductile mild steel, were due to insufficient notch toughness at the temperature prevailing at the time of the casualty, and to the tough/brittle-fracture transition temperature of the steel being too high.—U. E.

Quality Control of Spot Welded Assemblies. A. J. Hipperson. (*Welding Metal Fab.*, 1952, **20**, Apr., 143–145). Procedures in applying statistical quality control to spot welding are outlined.—U. E.

Dredge Welding Repairs. A. L. J. Beaumont. (*New Zealand Eng.*, 1952, **7**, Jan. 15, 29–32). A detailed step-by-step description is given of the procedure adopted to repair by welding a manganese steel bottom tumbler taken from a gold dredger.—P. M. C.

Welding of Alloy Steels. J. M. Rowland. (*New Zealand Eng.*, 1951, **6**, Sept. 15, 351, 352). The author points out that as the hardenability of a steel is increased by alloy additions, its weldability is diminished. Brief notes are given on the hardening effects of carbon, manganese, nickel, chromium, and molybdenum.—P. M. C.

The Wear Treatment of Machine Components. G. K. Calligan. (*New Zealand Eng.*, 1951, **6**, Sept. 15, 347–350). The advantages and applications of hard-facing by welding, the types of alloy most suitable for various service conditions, methods of deposition, and the relation between the results of hardness tests and actual resistance to abrasion, are discussed.—P. M. C.

New Process to Speed Turbine Blade Production. (*Steel Processing*, 1952, **38**, Mar., 133, 134, 141). A new process for fabricating jet engine turbine blades has been developed by the General Electric Company, U.S.A. Details are not given, although it is stated that the blade and base are made separately and welded together. Forging, previously a bottleneck, is now eliminated with a 55% reduction in cost and nearly 40% saving of scarce materials.—P. M. C.

Design and Operation of Electric Weld Pipe Mill at National Tube's National Works. G. C. Anderson. (*Iron Steel Eng.*, 1952, **29**, May, 96–101). This article describes the new 26–36 in. electric weld pipe mill of the National Tube Co., McKeesport. The mill produces large-diameter pipes in lengths up to 40 ft. The plate is first formed into a **U**-section in a 2000-ton press, after which it is closed in an 18,000-ton press. The welding is first carried out on an outside automatic welder after which the pipe is turned and the inside weld is made with the seam facing downwards. The pipe is passed through a hydraulic expander and tested.—M. D. J. B.

Hardfacing with Stellite. (*Machinery*, 1952, **80**, Jan. 31, 179–188). The methods employed by Deloro Stellite, Ltd., Birmingham, are described.—E. C. S.

Step Welding of Tool Steels and of Certain Alloy Constructional Steels. T. Norén. (*Jernkontorets Ann.*, 1952, vol. 136, Apr., 74–89). [In Swedish]. Time–temperature–transformation (TTT) diagrams make it possible to calculate suitable welding conditions for hard tool steels, thus decreasing their tendency to crack and facilitating heat-treatment. These principles were applied to the development of step welding. The method is described and its suitability for tool steels discussed. Step welding has not yet been applied to constructional steels, but it is suggested that their TTT diagrams will be useful in enabling correct preheating temperatures to be chosen.—G. G. K.

Stainless Clad Steels. H. Thielsch. (*Welding J.*, 1952, **31**, Mar., 142s–160s). This is a review of published and unpublished information on manufacturing methods, carbon diffusion, physical and mechanical properties, heat-treatments, welding procedure and processes, dilution, selection of welding electrodes, and flame cutting. (81 references).—U. E.

Schlieren Analysis of Inert-Gas Arc Shields. W. B. Moen and G. J. Gibson. (*Welding J.*, 1952, **31**, Mar., 208–212). The Schlieren method of making gases visible was applied to a study of helium and argon as arc shields with non-consumable electrode equipment. The results indicate that either gas will provide adequate coverage. The choice of gas depends only upon the desired electrical characteristics and economics. A brief explanation of the Schlieren apparatus and a number of photographs substantiating the results are included.—U. E.

Surface Conditioning of Structural Steel. R. E. Somers and H. C. von Blohn. (*Welding J.*, 1952, **31**, Mar., 193–198). Surface conditioning practices were applied, where necessary, to structural steels, and low-hydrogen electrodes were used on all grades of steel and no underbead weld cracking was found.—U. E.

Welding of High-Heat-Resistant Materials. J. L. Solomon. (*Welding J.*, 1952, **31**, Mar., 233–238). Improvements in the use of the three-phase seam and roll spot welder for welding nickel alloys and stainless and heat-resistant steels are discussed.—U. E.

Flash Butt Welding of High-Temperature Alloys. I. A. Oehler. (*Welding J.*, 1952, **31**, Mar., 230–232). Electrical and mechanical requirements for various joint sections are presented. A brief description of typical flash welding specifications is given.—U. E.

Welded Joints Between Dissimilar Metals in High-Temperature Service. R. W. Emerson and W. R. Hutchinson. (*Welding J.*, 1952, **31**, Mar., 126s–141s). The effect of time and temperature on the metallurgical properties of welded joints between austenitic and ferritic material using austenitic electrodes was studied. The rate of diffusion of carbon at 1050° F. is relatively slow, but high-temperature post-weld heat-treatment results in rapid carbon migration. In placing dissimilar metal joints in service, excessive temperature cycling should be avoided, particularly when the high-temperature side of the cycle is in the scaling range of the low-alloy ferritic steel.—U. E.

Hermetic Welding Stops Internal Corrosion. B. Gross. (*Iron Age*, 1952, **169**, Feb. 7, 142–144). The hermetic sealing of aircraft engine mounting rings has successfully stopped internal corrosion. Over 200,000 such parts fabricated at Rohr Aircraft Corp. since 1942 have given satisfactory service. Methods of detecting leaks in the assembly are described.

Evaluation of Tests for Steels for Welded Structures. P. J. Rieppel, R. G. Kline, and C. B. Voldrich. (*Welding J.*, 1950, **29**, Apr., 195s–212s). A study of the weldability of steels and the influence of welding upon the performance of large

welded structures is reported. Three bend test specimens and one notched-tension specimen were investigated, and results correlated with those obtained from full-scale specimens.

Deformations of Steel Girders as a Result of the Contraction of Longitudinal Arc Welds. R. Malisius. (*Mitteilungen aus den Forschungsanstalten des Gutehoffnungshütte-Konzerns*, 1940, **8**, Feb., No. 1, 15–28; Mar., No. 2, 29–40). By measurement of the curvatures of girders of St 37 steel produced by different methods of longitudinal welding, the effect on the girder curvature was discovered. Elongation measurements yielded numerical values for the size and distribution of the contractional stresses. The relation between girder curvature, contraction, and the contractional forces was established. The experiments gave a basis for predicting the girder deformation as a result of welding. Tests were made using St 52 steel to determine the effect of the properties of the material on the deformation.—R. J. W.

Concerning the Steel in the Hérenthals-Oolen Bridge and Its Weldability. H. Busch and W. Reulecke. (*Mitteilungen aus den Forschungsanstalten des Gutehoffnungshütte-Konzerns*, 1942, **9**, Feb., No. 6, 119–135). After the accident to the Hasselt bridge, there was damage to two further similar structures. Specimens were taken from various parts of the Hérenthals-Oolen bridge in May 1940 and this paper describes the work undertaken on them. The chemical composition, and mechanical properties, weldability, and the hardening and behaviour in weld-bending tests were determined.—R. J. W.

Safety and Health in Arc Welding. J. S. Kidd. (*New Zealand Eng.*, 1952, **7**, Mar. 15, 115–117).

Ultrasonics Make Soldering Easier. (*Iron Age*, 1952, **169**, Mar. 27, 105). A magnetostriction transducer is utilized to produce high-frequency vibrations in a soldering iron bit. When the bit is covered with solder and applied to a surface, erosion takes place and wetting or tinning is greatly facilitated.—A. M. F.

Industrial Brazing—Design and Strength of Brazed Joints. E. V. Beatson and H. R. Brooker. (*Welding Metal Fab.*, 1952, **80**, Apr., 146–149; May, 182–188). Joint designs used for welding and brazing are compared, and their applicability is discussed.—U. E.

How to Select Brazing and Soldering Materials. H. R. Clauser. (*Mat. Methods*, 1952, **35**, Mar., 105–120). This very comprehensive article presents a broad view of the types, characteristics, and uses of the available brazing and soldering materials.—P. M. C.

New Gas Cutting Techniques Aid Heavy Scrap Reduction. R. F. Helmkamp and A. H. Yoch. (*Iron Age*, 1952, **169**, Apr. 3, 135–137). Tables of the pressures required and gas consumptions for oxygen and acetylene for both hand and machine torch cutting are given. The use of an oxygen lance in co-operation with a hand torch is described; it has been used for cutting steel casting risers up to 90 in. in dia.—A. M. F.

Powder Cutting and Scarfing of Stainless Steels. C. W. Powell. (*Welding J.*, 1950, **29**, Apr., 308–310). A brief description is given of the powder-cutting method used for cutting 2½–4¼ in. thick stainless steel and heavy scrap sections. Scarfing, *i.e.*, removing surface defects with the oxy-acetylene flame, is only applied to salvage partially rolled ingots which have been rejected by the blooming mill because of poor surface.—U. E.

Oxygen Cutting of Defense Equipment Materials. A. H. Yoch. (*Welding J.*, 1952, **31**, Mar., 217–230). The oxygen gas-cutting process is described and illustrated.—U. E.

MACHINING AND MACHINABILITY

Contribution to the Study of the Wear of Cutting Tools. F. Eugène. (*Rev. Mét.*, 1952, **49**, Apr., 267–282). By a method of analogy it is shown that the type of wear encountered in practice is conditioned by cutting angle and speed and that these two factors affect the mode of plastic deformation of the worked material. A new method for measuring the intrinsic resistance to wear of cutting tools is described.

Machining Heavy Forgings in Germany. H. Dürr. (*Stahl u. Eisen*, 1952, **72**, June 5, 687–695). Machining of forgings represents a large fraction of operating costs in the production of heavy forgings. In some German works, machining consumes as much power as forging and heat-treating put together. Many of the machines employed are out-of-date. Heavier machine tools have been developed and some new ones are described.—J. P.

CLEANING AND PICKLING

Surface Preparation Values and Sandblasting Economics. A. J. Liebman. (*Corrosion*, 1950, **6**, May, 151–157). The author discusses the selection of the best method of preparing a metal surface to receive a corrosion-resistant coating and outlines all surface-preparation methods with emphasis on sandblasting. A proper terminology for sandblasting is necessary for the operator to interpret instructions. Selecting the sand for sandblasting is discussed.—H. F.

Rinsing for Electroplating. R. J. Rominski and F. L. Clifton. (*Amer. Electropl. Soc., 38th Annual Proc., Tech. Sessions*, 1951, July–Aug., 37–48). Spray rinsing in the cleaning cycle as well as during the plating cycle is recommended. A chromic-acid recovery flow diagram is given. Still tank rinses, spray rinses, and fog rinses are compared. The importance of water conditioning is mentioned, ion-exchange demineralization being cheaper than distillation.—T. E. D.

Rosin Amine–Ethylene Oxide Condensates as Corrosion Inhibitors for Mild Steel in Hydrochloric Acid. E. A. Bried and H. M. Winn. (*Corrosion*, 1951, **7**, June, 180–185). Applications of amines as hydrochloric acid inhibitors are discussed in detail and shown to result in greater use of inhibited acids for metal cleaning and oil-well acidizing. Ethylene oxide condensates of amines proved most effective as inhibitors. Experiments to establish the optimum ethylene oxide content are described.—E. C.

Pickling Stainless Steels. J. Bary. (*Mét. Constr. Méc.*, 1952, **84**, Apr., 259–261). The author discusses the hydrofluoric acid and ferric sulphate pickling baths, the main advantages of which are: (1) Rapid solution of oxides and retarded attack of metal; (2) complete absence of evolution of gases and acid vapours; (3) possibility of working between room temperature and 70° C.; and (4) lower cost than for hydrofluoric–nitric acid baths.—R. S.

A New Degreasing Evaluation Test: The Atomizer Test. H. B. Linford and E. B. Saubestre. (*Amer. Electropl. Soc., 38th Annual Proc., Tech. Sessions*, 1951, July–Aug., 169–179). A specially designed specimen, which always exhibits regular drainage, is used. The soiling and cleaning procedure is outlined, and the evaluation of cleanliness is described. Some results of cleaning index are given for samples soiled with lard oil and mineral oil.—T. E. D.

Perchloric Acid Hazard. E. Knuth-Winterfeldt. (*Metal Progress*, 1952, **61**, Mar., 80). Several electrolytes based on De Sy and Haemer's type (6% perchloric acid in alcohol) with inert material to prevent concentration by evaporation of dangerous derivatives have been successfully used for many years. No explosions have been encountered and details of a number of tests carried out on the solution are given.

A New Cell for Electrolytic Polishing—Characteristics of Its Use for Polishing Iron. A. Roos. (*Rev. Mét.*, 1952, **49**, May, 385–390). The cell has a mercury cathode and the polishing process is studied by following the speed of discoloration of the anodic oxide layer during inverse electrolysis. The results show that: (*a*) An increase in viscosity increases the polishing speed; (*b*) there is, for a given viscosity, an optimum oxalic acid content corresponding to maximum surface tension; and (*c*) with other variables constant, polishing speed is affected by the thermal treatment undergone by the metal.—A. G.

PROTECTIVE COATINGS

Spray and Sinter to Make High Temperature Alloy Parts. J. E. Cline, R. T. Thurston, and J. Wulff. (*Machinist*, 1952, **96**, May 10, 706–709). As the heat-resistance of alloys rises the machinability ratings usually decrease; hence forming and cutting may not be feasible. A solution to this problem is to spray the alloy on to a removable mandrel and thus build up the required shape. The process is described.—R. A. R.

The Metallizing Process. F. Keller. (*Welding J.*, 1952, **31**, Apr., 291–295). A brief review of the metallizing process, its field of application, apparatus, method used in preparation of surfaces, and the nature of the coatings is given.—U. E.

Modern Developments in Metallizing. S. Tour. (*Welding J.*, 1952, **31**, Mar., 199–207). The wire-melting gun method for coating metal surfaces is described. Improvements in design of wire guns, surface preparation, equipment, technique, and types of coating are discussed. Sprayed metal surfaces form a good base for the application of organic coatings.—U. E.

Development of Fused Metallized Coatings. H. S. Sayre. (*Welding J.*, 1952, **31**, Jan., 35–39). Fused metallized coatings as developed by the U.S. Naval Service consist of a sprayed coating of a corrosion-resistant alloy on to steel. The coating is then remelted with an oxy-acetylene flame, and, after controlled cooling, is polished. The technique is described together with some successful results obtained with propeller blades and shafts of outboard motors.—R. A. R.

Metal Spraying in Protective Gas Atmospheres. H. Reininger. (*Metalloberfläche*, 1951, **5**, Mar., A42–A43). The technical and economic aspects of using protective gases in metal spraying are discussed. It is concluded that at present the cost of the equipment required would be uneconomic.—P. F.

Metallizing Cuts Marine Maintenance Costs. F. M. Earle. (*Iron Age*, 1952, **169**, Jan. 31, 103–105). Aluminium and zinc sprayed on properly cleaned iron and steel and coated with vinyl seal has been found to protect effectively fishing boats from salt-spray corrosion. Reduced docking and maintenance costs are said to have offset the initial expense in two years.—A. M. F.

Hard-Facing for Impact. H. S. Avery. (*Welding J.*, 1952, **31**, Feb., 116–143). Static and dynamic compression testing are described together with data for important hard-facing alloys such as martensitic iron and steel and austenitic steel. Certain critical limitations of impact energy and velocity are suggested to minimize structural damage and to help in alloy selection. A detailed description is given of three hard-facing alloys with their applications and the advantage of composite structures to control wear is shown.—U. E.

Iron Plating Restores Worn Parts. R. Pyles. (*Iron Steel Eng.*, 1952, **29**, Jan., 151–152). A description is given of the Van der Horst method of electrodepositing iron. The newly deposited electrolytic iron develops a fine, closely packed columnar structure with the grains perpendicular to the surface of the metal underneath. Details are given of the plating bath, plating procedure, properties of the deposit, and typical applications.

Phosphatization of Metals. J. Bary. (*Ingénieurs et Techniciens*, 1951, Dec., 621–623). The use of phosphatization for the protection of metal surfaces from corrosion is discussed. Details are given of methods of treatment.—T. E. D.

Protection from Corrosion by the Cold Phosphatization Method. V. S. Lapatukhin. (*Zhurnal Prikladnoi Khimii*, 1951, **24**, 373–382). [In Russian]. A new method of producing phosphate coatings at room temperature is proposed. The reagent used contained, in addition to the usual components, fluorine compounds and accelerator. The latter lower the free acidity of the solution and reduce the depolarization of hydrogen evolved during the process. Optimum conditions for the chemical formation of phosphate coatings at room temperature and cold electrophosphatizing with alternating current were worked out. Good protection against corrosion, and economy are claimed.—V. G.

Phosphatizing and Its Scientific Fundamentals. W. Machu. (*Metallwirtschaft*, 1943, **22**, Oct. 20, 481–487). Descriptions are given of the historical development of phosphatizing, the reactions that occur during the formation of a phosphate layer, the porosity of the coating, and electrochemical porosity testing methods. An investigation of the course and endpoint of phosphatizing is reported. The theory and practice of different accelerating media are developed, and the technical operation of phosphatizing by dipping and spraying methods described.—R. J. W.

Phosphate Coatings for Military Products. N. P. Gentieu. (*Product Eng.*, 1952, **23**, Feb., 183–190). The author reviews the types of phosphate coating now specified by the American armed services for protection of metal surfaces. The coatings improve paint adhesion, provide corrosion protection, increase the lubrication of friction surfaces, facilitate forming, and can serve as decorative finishes on some products. A large chart covering the specifications is given to aid in the selection of phosphate finishes.—A. M. F.

Phosphatizing—Versatile Substitute for Metallic Coatings. (*Steel*, 1952, **130**, Jan. 28, 64–66). The principle of operation of both zinc phosphate and manganese phosphate coatings is described. These coatings are being more widely used for paint bonding, corrosion prevention, wear resistance, and as a lubricant in cold extrusion. They can also be used to prevent scratches and die marks in forming operations.

Ceramic Coatings. (*Aircraft Prod.*, 1952, **14**, Mar., 107–108). Recent U.S. developments for extending the life of high-temperature alloys are reviewed, and the composition, application, and performance of U.S. National Bureau of Standards ceramic coating A-417 are given.—T. E. D.

Overhead Oven Speeds Finishing and Reduces Costs at Atlas Mfg. Company. (*Indust. Heating*, 1951, **18**, Nov., 2055–2062). The ovens and oven-heating equipment for stoving sprayed coats of paint on boiler jackets have been installed on the factory roof. The conveyor system, running at 10 ft./min. has increased production by 25%. The conveyor, after leaving the loading area, first rises and enters the 'burn-off' oven (600° F. for 6 min.) and then passes through paint-spray booths on a lower level and finally up into the finish bake oven (350° F. for 8 min.). Both the burn-off oven and the stove are gas heated.—B. G. B.

A Student's Approach to the Theory and Practice of Vitreous Enamelling. J. H. Gray. (*Sheet Metal Ind.*, 1951, **28**, Sept., 853–864; Nov., 1043–1050; Dec., 1139–1146; 1952, **29**, Apr., 361–366).

Recent Ceramic Coatings for High-Temperature Alloys. (*Ceramics*, 1952, **3**, Jan., 581–585). **Ceramic Coatings for Metal Protection at High Temperatures.** (*Product Eng.*, 1951, **22**, Nov., 177–179). Three high-temperature types of ceramic coating developed by the National Bureau of Standards are described. Coated Inconel plates have been successfully exposed to temperatures as high as 2200° F. without damage. Some of the present difficulties are brittleness of the coat and the high firing temperatures required.—A. M. F.

Ceramic Coating Increases Life of Engine Exhausts. W. G. Hubbell. (*Metal Progress*, 1951, 60, Dec., 87–91, 166). **Ceramic Coatings Can Save Critical Alloys.** W. G. Hubbell. (*Iron Age*, 1951, **168**, Nov. 22, 81–85; *Aircraft Prod.*, 1951, **13**, Nov., 357–362). A series of tests conducted by the Ryan development laboratories has shown that ceramic coatings will extend the life of stainless-steel exhaust components at the elevated temperatures encountered in aircraft service. No deterioration by oxidation, carbon absorption, or corrosion attack was found in test periods running up to 1623 hr., as against deterioration in 650 hr. without the coating.—A. M. F.

Recent Ceramic Coatings for High-Temperature Alloys. (*Indust. Heating*, 1951, **18**, Dec., 2263–2268, 2320–2322) A new coating, composed of a high-barium alkali-free frit (glass) with a 30% admixture of chromic oxide, is described. Details of the preparation and application are given; a uniform green finish 0·001–0·002 in. thick can be obtained. The surface to be coated is first cleaned by pickling or sand-blasting. This coating retards carbon absorption, corrosion by lead compounds, and oxidation of exterior surfaces. It is resistant to temperatures of about 1650° F. and to thermal shock, and adheres well to all surfaces including edges and welds.—B. G. B.

Islanding—A Surface Characteristic of Some Porcelain Enamels. M. K. Blanchard and W. A. Deringer. (*J. Amer. Cer. Soc.*, 1952, **35**, Jan., 12).

Relative Importance of Various Sources of Defect-Producing Hydrogen Introduced into Steel During the Application of Porcelain Enamels. D. G. Moore, M. A. Mason, and W. N. Harrison. (*J. Amer. Ceram. Soc.*, 1952, **35**, Feb. 1, 33–41: *Ceramics*, 1952, **4**, Mar., 40–43). Deuterium (heavy hydrogen) has been used to trace the source of defect-producing hydrogen in steel. The deuterium was introduced, in turn, as a replacement for protium (normal hydrogen) in the pickling acid, milling water, the quenching water, the chemically combined water in the clay, and the dissolved water in the frit. The gases evolved during fish-scaling of the coated steel specimens from each experiment were collected and analysed with the mass spectrometer. The data showed that the principal source of defect-producing hydrogen was the dissolved water in the frit.

X-Ray Study of the Reactions at the Steel Surface When Titania Enamel Is Applied Directly. G. S. Douglas and J. M. Zander. (*J. Amer. Ceram. Soc.*, 1952, **35**, Jan., 5–11).

Catalytic Combustion of Atmospheric Contaminants in Effluent from Wire Enameling Ovens. A. E. Goss. (*Wire and Wire Products*, 1952, **26**, Nov., 1051–1055, 1089–1090). The paper is based on an investigation by the Connecticut State Department of Health of a catalytic combustion process applied to the fumes from wire enamelling stoves. Particulars of the plant are given and results of the analysis of samples are quoted. Subject to correct adjustment of operating conditions, effective control of atmospheric pollution is possible.

The Scratch Resistance of Sheet Iron White Covercoats. F. G. Morriss. (*Bulletin of the Institute of Vitreous Enamellers, Ltd.*,

1952, **3**, Apr.: *Foundry Trade J.*, 1952, **92**, Apr. 3, 367–370). A simple scratch-testing apparatus, incorporating a diamond fixed to a long, freely pivoted, and counterbalanced arm, is described. A range of covercoats was tested, and the preliminary results indicated that the bubble structure of enamel coatings greatly affects the scratch resistance.—T. E. D.

Application of Dilatometric Analysis to the Problem of Enamelling Cast Iron. A. Le Thomas and P. Tyvaert. (*Fonderie*, 1951, Nov., 2685–2691). Linear coefficients of expansion of cast iron and enamel are calculated and compared, using the Chevenard differential dilatometer. The nature of the test specimens is described.—R. S.

Some Investigations of the Abrasion of Paint. N. A. Brunt. (Conf. on Abrasion and Wear, Delft, Nov., 1951: *Engineering*, 1952, **173**, Apr. 18, 504–506). The author distinguishes between abrasion by impact and abrasion by friction. The former is essentially dependent on the value of the modulus of elasticity and the ultimate strength of the material. With a low modulus, maximum stresses will be low and consequently there will be no abrasion. The latter is a matter of hardness and plastic behaviour. From the experiments described, it appears that the soft and weak oil paints have a very high abrasion resistance and that the hard, physically drying materials are non-resistant. The paints with the greatest percentage of plastifier are the most resistant.

POWDER METALLURGY

Some New Developments in the Pressing of Powders. C. Agte and M. Petrdlík. (*Hutnické Listy*, 1952, **7**, Mar., 121–124). [In Czech]. A new conception of the laws of compression of powdered materials is presented, based on the equations and diagrams proposed by J. M. Balšina between 1936 and 1948. A study of the pressing process is made and illustrated by photographs of specimens consisting of compressed lead spheres, in the compressed, fractured, and crumbled states. The density of arbitrary powders, mainly metallic, is derived theoretically, as a function of the applied pressure.—P. F.

The Effect of Particle Size on the Behaviour of Sintered Iron on Nitriding. W. Köster and J. Raffelsieper. (*Arch. Eisenhüttenwesen*, 1952, **23**, May–June, 225–228). The effect of particle size of the initial material on the nitriding of sintered iron compacts in ammonia has been investigated. The specimens made from fine-grained powder took up more nitrogen than those from coarse powder, but the volume expansion was greater with the coarse-grained material. The increase in hardness during absorption of nitrogen was greater with the fine-grained specimens, but bend strength, which decreases with nitrogen content, is somewhat better with the fine-grained than with the coarse-grained material. The resistance to abrasion increases with the nitrogen content up to 1% nitrogen and then remains essentially constant. The machinability of sintered and nitrided compacts is limited by the brittleness of the nitride phases. The corrosion resistance to H_2SO_4, NaCl solution, and water vapour decreases markedly as the nitrogen content increases.—J. P.

Stainless Steel Powder for Mechanical Parts. A. H. Grobe. (*Product Eng.*, 1951, **22**, Dec., 168–172). Powder of stainless steel type 316 is now produced by disintegrating the molten steel with a jet of water. Carbide die parts are necessary because of the high pressure applied in compacting. The sintering atmosphere must be highly reducing and its dew point very low. The powder can be successfully sintered at as low as 2100° F. with tensile strengths after single sintering treatment from 17,000 to 29,000 lb./sq. in. and 5–6% elongation. Data on density and strength at different sintering temperatures and compacting pressures are given.—R. A. R.

Precision in the Manufacture of Mechanical Parts. M. Chalvet. (*Rev. Gen. Méc.*, 1952, **36**, Mar., 83–92). Precision methods are based on deformation of metal or machining. Improved economy is achieved if precision parts can be obtained direct by deformation without cutting away metal. This has led to the new techniques of powder metallurgy, and precision and pressure casting which the author reviews. He also considers superfinish machining.—R. S.

PROPERTIES AND TESTS

Notch Sensitivity at High Temperatures Evaluated. W. F. Brown, jun. and G. Sachs. (*Iron Age*, 1952, **169**, Mar. 20, 91–95). A National Advisory Committee for Aeronautics survey shows that at short times to rupture, the strength of notched low-alloy bolting and boiler steels at 932° F. exceeds that of unnotched bars. The amount depends primarily on the depth of notch. With increasing time to rupture, notched strength falls well below unnotched specimens, until 30,000 hr. is reached, when notched bars may recover. Low original ductility means low notch strength, and heat-treating to raise creep strength increases notch sensitivity. (13 references).

A. M. F.

Lower Alloys Developed for High Temperature Use. C. L. Clark. (*Iron Age*, 1952, **169**, Feb. 21, 98–102). Timken 17–22A(S), a low-alloy steel originally developed for high-temperature bolt application, has been modified by reducing the carbon, increasing the vanadium, and adding tungsten, for use at 1100° F. This steel has a minimum rupture strength of 30,000 lb./sq. in. for periods of 1000 hr. at high stress. Contrary to British experience, previous normalizing at high temperature does not increase the high-temperature strength of the lower alloys, but this treatment does increase the strength of 12% Cr stainless grades.—A. M. F.

Behaviour of Steels at Superheated Steam Temperatures of up to 1100° F. G. Bandel and H.-J. Wiester. (*BWK.*, 1949, **1**, 8, 203–208). Literature on the high-temperature creep strength of steels is reviewed and data on the rupture strength for 10,000 hr. at 600 and 700° C. and the creep limit (German DVM test) at 600 and 700° C. for the following heat-treated austenitic steels are presented: (1) 18/9/0·5 Cr–Ni–Ti steel; (2) 18/9/0·4 Mn–Cr–Ti steel; (3) 18/8·5/1/0·3 Cr–Ni–W–Ti steel; (4) 18/12/0·7/0·2 Mn–Cr–V–N_2; and (5) 14/30/1·8 Cr–Ni–Ti steel. Of these, steels (3), (4), and (5) have a rupture strength for 10,000 hr. of 35,600 lb./sq. in. It is concluded that it is not safe to extrapolate short-time test data with the object of predicting the behaviour of steels for long-time service at 600° C.—R. A. R.

Addendum

With reference to the abstract 'Effect of Cold Drawing on Creep-Resisting Molybdenum Steel Steam Pipe' (July *Journal*, p. 334), it should be added that the two steels tested also contained Cr 0·9% and V 0·2%, respectively.

Contribution to the Study of Creep of Boron Steels. G. Delbart and M. Ravery. (*Comptes Rendus*, 1950, **231**, Dec. 18, 1498–1500). Creep tests on a 0·30% carbon steel with and without additions of 0·009% and 0·015% of boron are reported. The boron-free steel had a better creep limit than the boron-alloyed steels, the microstructure of which revealed a precipitate in the grain junctions.—R. A. R.

How to Select Steels for Low Temperature Service. J. L. Everhart. (*Mat. Methods*, 1952, **35**, Jan., 75–79). The sub-zero properties and the embrittlement temperatures of steel are affected markedly by both metallurgical and mechanical factors. The author briefly discusses the effects of melting procedure, composition, rolling temperature, grain size, heat-treatment, type of loading, design, size, and shape of component, and the presence or absence of notches. The low-temperature properties of 11 different steels are tabulated, and hints on their most useful temperature ranges are given. (17 references).—P. M. C.

The Importance of Hydrogen in Steelmaking. K. C. Barraclough and C. Sykes. (*Inst. Hierro y Acero*, 1952, **5**, Jan.–Mar., 282–297). [In Spanish]. Investigations in the Brown-Firth Research Laboratories into the determination of hydrogen in steel and on the effects of hydrogen in liquid and solid steel are reviewed. By using vacuum heating for the determination of hydrogen in solid steel, it has been possible to find a simple method for the estimation of hydrogen in liquid steel baths. Results have indicated a way of reducing the hydrogen in electric arc furnace steels. They have also shown the limits of hydrogen content in liquid steel which would probably produce ingots free from porosity. Whilst, in stainless steels, the hydrogen content in the solid state appears to have little effect on the mechanical properties, the presence of hydrogen in ordinary and low-alloy steels has an embrittling effect. By a systematic study of low-alloy steel samples hydrogenated to varying degrees by high pressure, a quantitative appreciation of this embrittling effect is obtained. The current theory that hydrogen causes flakes appears to be confirmed though there have been cases in which high

hydrogen contents have not caused cracks. The only safe thing to do is to reduce the hydrogen content as far as possible. The main precautions are: (1) Avoid moisture; (2) charge only dry materials; and (3) preheat scrap which is excessively oxidized.—R. S.

Effect of Hydrogen on Graphitization. F. Brown and M. F. Hawkes. (*Amer. Found. Soc. Preprint*, 1952, No. 5). Hydrogen was found to inhibit graphite formation (1) during solidification of the eutectic, (2) by gamma-range graphitization of cementite, (3) by rejection on cooling along the A_3 line, (4) by the decomposition of austenite by the stable eutectoid reaction, and (5) by the alpha-range graphitization of cementite. However, the reactions may still proceed to a limited extent. The mechanism of the stabilizing of carbides is unknown, but hydrogen does not retard carbon diffusion.

Solubility of Gaseous Nitrogen in Gamma Iron and the Effect of Alloying Constituents—Aluminium Nitride Precipitation. L. S. Darken, R. P. Smith, and E. W. Filer. (*Trans. Amer. Inst. Min. Met. Eng.: J. Met.*, 1951, **3**, Dec., 1174–1179). The authors have determined the solubility of nitrogen gas in the γ-phases of purified iron and low-alloy steels. In plain low-carbon steels and iron, the solubility may be represented by the equation: solubility (% N) = $0{\cdot}0404 - 1{\cdot}2 \times 10^{-5} \times$ temperature (° K.). From the rate of approach to equilibrium, the diffusivity of nitrogen has been determined, and the solubility of aluminium nitride, the precipitation of which has been disclosed in a study of aluminium-killed steels held in nitrogen, has also been measured.—G. F.

A Thermal and Microscopic Study of the Iron–Carbon–Silicon System. J. E. Hilliard and W. S. Owen. (*J. Iron Steel Inst.*, 1952, **172**, Nov., 268–282 [This issue].

Effect of Quench Ageing on Strain Ageing in Iron. A. H. Cottrell and G. M. Leak. (*J. Iron Steel Inst.*, 1952, **172**, Nov., 301–306). [This issue].

The Formation of Bainite. T. Ko and S. A. Cottrell. (*J. Iron Steel Inst.*, 1952, **172**, Nov., 307–313). [This issue].

Surface Active Components of Steel. S. M. Baranov. (*Doklady Akademii NAUK S.S.S.R.*, 1952, **83**, 1, 125–128). [In Russian]. The influence of surface active, colloidal dispersed silicates on the properties of steel is discussed. Experimental evidence indicates that the variations in properties of steel from the same heat can be explained by surface phenomena accompanying crystallization processes caused by silicates.—V. G.

Tin and Copper in Steel: Both Are Bad, Together They're Worse. S. L. Gertsman and H. P. Tardif. (*Iron Age*, 1952, **169**, Feb. 14, 136–140). Hot bend tests carried out on steels (C, 0·15–0·20%) with varying contents of tin and copper are described. Tin above 0·05% caused cracks in the bend test at 1825° F., heating in an argon atmosphere prevented this up to a tin content of 0·5%. Deeper surface breaks developed as copper was increased at a given tin level and as tin was increased at a given copper level.—A. M. F.

Alloy Conservation: Boron Fills the Bill. A. H. d'Arcambal. (*Steel*, 1952, **130**, Feb. 18, 107–108). The advantages and limitations of available boron steels as replacements for other alloy steels are discussed. To obtain the proper hardenability effects of boron the steels must be correctly quenched and tempered. Too high a percentage of boron can cause hot-shortness and, for boron-treated carburizing steels, consideration should be given to lowering the carbon content to combat warping.—A. M. F.

Boron Steel Development. D. L. McBride. (*S.A.E. J.*, 1952, **60**, Mar., 26–34). This is a review of the properties and uses of boron steels. Hardenability and notch toughness are considered in detail. (11 references).—P. M. C.

Hardenability as the Criterion for Selecting Boron Steels. H. B. Knowlton. (*Mat. Methods*, 1952, **35**, Mar., 84–87). The author discusses the choice and use of the new boron steels with particular reference to the effects of heat-treatment and case-hardening on their hardness, toughness, and fatigue properties.—P. M. C.

Substitution of Boron for Alloys in Cast Steels. R. A. Dyke, jun., and C. K. Donoho. (*Amer. Found. Soc. Preprint*, 1952, No. 45). Boron in cast steels increases the hardenability of carbon and low-alloy steels in the same way as it does in wrought steels. Tempering at as high as 1200° F. destroys the effect of the boron. One useful application that is discussed is the substitution of boron for the alloying elements in a Cr–Ni–Mo steel.—E. T. L.

On the Solution of Diffusion Problems Involving Concentration-Dependent Diffusion Coefficients. C. Wagner. (*Trans. Amer. Inst. Min. Met. Eng.: J. Met.*, 1952, **4**, Jan., 91–96). The author presents solutions of the differential equation for concentration distribution in diffusion couples and for the diffusion of an alloying element to or from the surface of a sample, for the case where the diffusion coefficient is an exponential function of the concentration of one component.—G. F.

Influence of Phosphorus on Hot Tear Resistance of Plain and Alloy Gray Iron. J. C. Hamaker, jun., and W. P. Wood. (*Amer. Found. Soc. Preprint*, 1952, No. 30). A procedure has been developed for tensile testing grey iron shortly after solidification, under conditions promoting hot tearing. Grey iron with 0·04–0·16% P was found the most susceptible to hot tears in the range 1900–1800° F.—E. T. L.

An Investigation of the Rôle of Aluminium in the Graphitization of Plain Carbon Steel. A. M. Hall and E. E. Fletcher. (*Trans. Amer. Soc. Mech. Eng.*, 1951, **73**, Aug., 743–750). The rôle of aluminium in the breakdown of iron carbide in plain carbon steels into iron and graphite at temperatures below the lower critical point of the steel was investigated. Carbides were extracted from plain carbon steels which had been deoxidized with different amounts of aluminium and had different known degrees of susceptibility to graphitization. The carbides were then examined and analysed with particular attention to the amount and form of the aluminium therein. By comparing the aluminium in the carbide with that in the original steel, data were obtained on the effect of aluminium on graphitization. The results suggest that a graphitization-resistant plain carbon steel differs little fundamentally from one which graphitizes readily. It is suggested that, in order to promote graphitization, the aluminium is either in solid solution in the ferrite matrix or that it fixes the nitrogen as AlN. Which of the forms taken could not be judged from the results. Any aluminium associated with the carbide phase probably does not influence the degree of susceptibility to graphite formation.—D. H.

Stainless Steels Resistant to Corrosion and High Temperatures. A. Gimenez y Sanmartin. (*Dyna*, 1951, **26**, Nov., 289–300; 1952, **27**, Jan., 1–12; Feb., 25–38). [In Spanish]. These steels are discussed in three groups: (*a*) Martensitic and ferritic chromium steels; (*b*) austenitic 18/8 type; and (*c*) heat-resisting steels. In each group, the composition, heat-treatments, physical and mechanical properties, and applications are given.—R. S.

Influence of Heat-Treatment on the Properties of Drawn Steel Wires. H. Wedl. (*Berg- Hüttenmänn. Monatsh.*, 1952, **97**, Mar., 51–57). Structural changes accompanying patenting were examined in 0·48 and 0·74% carbon steels. The required austenitizing temperatures were determined by quenching experiments, and the **S**-curves were also constructed. The influence of the austenitizing temperature on the position of the curves and on the resulting structure was thus elucidated. Conditions for the formation of sorbite only were established, and the influence of structural differences, obtainable by altering the furnace temperatures, on the tensile strengths of the wires was explained. The rôle of increasing proportions of ferrite on the strength was studied. In the course of works experiments on 0·34, 0·53, and 0·69% carbon steels, the effects of annealing before drawing, on the final strength of the wires were measured, and the strengths of the wires compared with similar, patented wires. Strengths of annealed, cold-worked, and patented wires were also compared with strengths of double-patented wires of similar composition, and the behaviour of the ferrite–cementite structure during cold work was observed. It is assumed that the strength is derived from: (*a*) Work-hardening of the slip planes, (*b*) obstruction to slip on the ferrite–cementite boundaries, and (*c*) the actual strength of the cementite. A type of deformation unaccompanied by work-hardening was also observed, and explanations for this are advanced.—P. F.

Spring Steels, Their Development, Properties and Sphere of Application. S. Ammareller. (*Stahl u. Eisen*, 1952, **72**, Apr. 24, 475–488). The development of spring steels, the preparation of the springs and their applications are described. Silicon steels (Si 1·5–2·2%, Mn 0·5–1·0%) have been used extensively in Europe. They are characterized by a high elastic limit and a lack of susceptibility to heat-treatment troubles. Tests have shown that steels with 1·5–2·0% Si and 0·5–0·7% Mn suffer less rim decarburizing than those

with higher manganese contents or with manganese or chromium and vanadium alone. Since a martensitic structure over the whole cross-section is a necessity, this fact is of great importance in spring making. The earlier view that a fibrous fracture was a criterion of the best spring steel has been confirmed by results of alternating fatigue tests. The influence of heat-treatment, cross-section, and surface finish on fatigue has been tested. With some steels the difference in fatigue strength between polished and as-rolled specimens was as high as 50%.—J. P.

SAE Charts Simplify Selection of Heat-Treatable Steels. E. H. Stilwill. (*Iron Age*, 1952, **169**, Feb. 14, 130–133). The Iron and Steel Technical Committee of the Soc. Automotive Engineers has drawn up a series of tables for the selection of heat-treatable steels. These tables relate the stresses present, the section size, and the quenching medium. All the S.A.E. H-band non-carburizing steels are included, as well as boron steels, where satisfactory hardenability data are available.—A. M. F.

Influence of Nitrogen on the Notch Toughness of Heat-Treated 0·3-Percent-Carbon Steels at Low Temperatures. G. W. Geil, N. L. Carwile, and T. G. Digges. (*J. Res. Nat. Bur. Stand.*, 1952, **48**, Mar., 193–200). Charpy impact tests were made at temperatures from 100° down to − 196° C. on fully hardened and tempered specimens of 0·3% carbon steels with different nitrogen contents. The transition from ductile to brittle fracture was affected by the amount and the form of the nitrogen. Fixation of the nitrogen as aluminium nitride was beneficial and the aluminium-treated steels had considerably lower transition temperatures than those without aluminium.—R. A. R.

A New Theorem in the Classical Theory of Elasticity. A. S. Lodge. (*Nature*, 1952, **169**, May 31, 926–927). A transformation is described by which the solutions of problems concerning the equilibrium of isotropic solids are made to yield solutions for corresponding problems for certain anisotropic solids.

The Dynamic Theory of Yield. K. E. Puttick and M. W. Thring. (*Iron Steel*, 1952, **25**, May, 155–159). This article is concerned with the relationship of the yield point of a metal, the condition of which is specified completely, to the temperature and strain rates of subsequent tests. From the postulate that the yield-point energy is given by the combination of energies due to strain and thermal vibrations, an equation for this relationship is derived. The theories put forward by others are discussed, particular attention being given to the 'carbon atmospheres' theory of yield given by Cottrell and Bilby. Recent work on tensile and compressive impact tests, brittle fracture, and the onset of twinning, is reviewed in the light of the theory.—G. F.

Dislocation Collision and the Yield Point of Iron. A. N. Holden. (*Trans. Amer. Inst. Min. Met. Eng.: J. Met.*, 1952, **4**, Feb., 182–188). The primary object of the investigation described was to extend Cottrell's dislocation theory of local yielding to account for local cataclysmic flow by a dislocation collision process, and to give experimental evidence to support such a process. Only the yielding of iron containing carbon is discussed. There is no experimental evidence that polygonization exists in strain-aged crystals of iron possessing a yield point, whilst the Cottrell mechanism is not inconsistent with the absence of a yield point in annealed single crystals. Some small strain is required to introduce sufficient dislocations in annealed iron crystals to cause a yield point, but in polycrystals sufficient dislocations are already present at the grain boundaries.—G. F.

The Mechanism of the Tensile Deformation of Single Crystals. F. Röhm and J. Diehl. (*Z. Metallkunde*, 1952, **43**, Apr., 126–128). Stress–strain curves of face-centred cubic crystals often show a steep rise after a gradual initial increase. The orientation dependence of the strain of the steep portion indicates that the increased rate of work-hardening is to be ascribed to the coming into action of an additional slip system. If it is assumed that the initial part of the curve is to be associated only with strain-hardening due to slip deformations, experimental evidence corroborates the assumption of the onset of double slip with the rise in the slope of the stress–strain curve. Pure aluminium crystals were used in the tests reported.—P. F.

On the Influence of Temperature and Deformation Velocity on the Critical Shear Stress. F. Vitovec. (*Z. Metallkunde*, 1952, **43**, Apr., 128–130). For low values of the yield stress the Becker-Orowan relation is applicable, but is inadequate at higher shear stresses. It is shown that, in evaluating the energy of activation, the square of the difference between the experimental shear strength at the absolute zero and the externally applied shear stress is not the correct measure to use, but that the difference should be taken between the squares of the separate terms. The new equation is functionally in good agreement with experimental results on the temperature and velocity dependence of the critical shear stress, without necessitating the introduction of a stress concentration factor.—P. F.

The Crystallographic Aspect of Slip in Body-Centred Cubic Single Crystals. II—Interpretation of Experiments. A. J. Opinsky and R. Smoluchowski. (*J. Appl. Phys.*, 1951, **22**, Dec., 1488–1492). A new method for determining the ratios of the critical shear stresses has been applied to test data. Any method which determined the slip plane and the orientation of the tensile axis gave useful results. In all instances where the data were available, the ratio of the critical shear stress on {123} and {112} was very close to unity, and did not change with temperature.—R. A. R.

The Application of the Plastic Theory to the Design of Mild Steel Beams and Rigid Frames. F. A. Partridge. (*Mech. World*, 1952, **131**, Mar., 102–108).

Relationships between Stress and Deformation Gradients. III—Notch Susceptibility. F. Bollenrath and A. Troost. (*Arch. Eisenhüttenwesen*, 1952, **23**, May–June, 193–201). The effects of stress and deformation gradients are considered mathematically and formulæ are derived to indicate the influence of non-uniform stress and distortion in hindering plastic deformation and to describe the dependence of fatigue strength on deformation gradient. The endurance limit of notched specimens can be estimated from a knowledge of tensile and tensile-compression strengths.—J. P.

On the Stresses in a Notched Strip. C.-B. Ling. (*J. Appl. Mech.*, 1952, **19**, June, 141–146). In a previous paper, a theoretical solution for notched strip under longitudinal tension required the solution of an infinite system of linear equations; this labour can be diminished by adapting a process known as 'the promotion of rank' which is described. The process is applied to solve the problem of a notched strip under transverse bending.—R. A. R.

The Texture of Metals after Plastic Deformation. P. Bastien. (*Métaux–Corrosion–Indust.*, 1952, **27**, Mar., 95–106). The influence of cold and hot working on the mechanical properties of steels is reviewed. The effect of plastic deformation on the crystalline structure is described for deformations produced by cold and hot working. Examples are also given of the special type of deformed structure obtained with polycrystalline materials having a fibrous structure.—B. G. B.

Instruments for Photoelastic Measurements. A. Martin. (*Métaux–Corrosion–Indust.*, 1952, **27**, Mar., 135–142). An outline of the method is first given and the optical principles of four different types of instrument are shown. One of the instruments, in which the polarized beam is produced by reflection from a mirror, is capable of extreme accuracy.

Electrical Resistance Strain Gauges. M. J. Sargeaunt. (*Machinery Lloyd* (*European Edition*), 1951, **23**, Oct., 53–63). The principles and use of this type of strain gauge are described. Different types of recording equipment associated with the use of strain gauges are illustrated. A new type of bonded resistance-wire gauge which is self-compensating for variations in the ambient temperatures has been developed.—B. G. B.

The Electrical Resistance Strain Gauge as a Problem in Electrical Measuring Technique. C. Rohrbach. (*Arch. Eisenhüttenwesen*, 1952, **23**, May–June, 239–243). The procedures for resistance strain gauge measurement are illustrated and compared. Some typical measuring instruments are described.

A Bridge Circuit for Wire Resistance Strain Gauges. B. V. Hamon. (*J. Sci. Instruments*, 1952, **29**, Feb., 53–54). The design details are given for a bridge circuit using Waidner-Wolff adjustable resistance elements in the measuring arm instead of the usual slidewire. Provision is made for the measurement of resistance changes from 0·1 to 1% of the gauge resistance with an accuracy of ± 0·1%.—H. D. W.

Heat Treated Brittle Coating Increases Sensitivity. A. J. Durelli and S. Okubo. (*Product Eng.*, 1951, **22**, Dec., 144–147). To increase the sensitivity of coatings used for stress analysis extra sensitive coatings have been used, but these tend to 'craze' during curing. Heat-treatment during the first part of the curing decreases the temperature at which crazing starts and at the same time increases the final sensitivity of the coating. The results of a systematic study of

the influence of heat-treatment on coating behaviour are reported.—R. A. R.

Plastics for Photoelastic Analysis. J. P. Vidosic. (*Proc. Soc. Exper. Stress Anal.*, 1952, **9**, 2, 113–124). Twelve different types of transparent plastic, including the well-established grades like BT-61-893, Catalin, and CR39 and several quite obscure varieties, were subjected to a series of tests to evaluate their usefulness for photoelastic experiments. The test methods are described and the results tabulated.—P. M. C.

Techniques in Residual Stress Analysis. W. Leaf. (*Proc. Soc. Exper. Stress Anal.*, 1952, **9**, 2, 133–140). The article is concerned with the method based on measurement of change in curvature of the surface as metal is successively removed from the opposite face in parallel layers. Mattson's method of stress calculation is described, and the author gives details of measuring techniques and discusses methods of metal removal.—P. M. C.

X-Ray Stress Measurements. C. W. Tucker, jun., and H. V. Anderson. (*Proc. Soc. Exper. Stress Anal.*, 1952, **9**, 2, 67–74). The X-ray method of residual stress measurement is described. It consists of making a number of measurements by the back-reflection technique of a particular interplanar spacing at a point on the surface of a metal specimen, calculating strains from these spacing values, relating these to the principal strains in the surface by elastic theory, and then calculating the principal stresses from the principal strains.—P. M. C.

A Basic Amplifier for Electronic Instrumentation. A. B. Jacobsen. (*Proc. Soc. Exper. Stress Anal.*, 1952, **9**, 2, 43–50). A basic amplifier is described which has been devised to provide the main elements required in electronic instrumentation. With this unit a variety of electronic instruments may be assembled from a few basic circuits, thereby reducing the cost of electronic instrument development for research. Circuit details, physical arrangements, and electrical characteristics of the amplifier are discussed, and possible uses are described. P. M. C.

Production Problems—XIV: Hard and Soft Blake Hooks. (*Iron Steel*, 1952, **25**, Apr., 129–130). A description is given of an examination carried out to determine the cause of a variation in hardness of fully processed Blake hooks. The variation is found to be due to differences in the amount of decarburization and the variable degree of solution of the carbides.—G. F.

Production Problems—XV: Generator Shaft Failures. (*Iron Steel*, 1952, **25**, May, 170). A brief description is given of an examination of three generator shafts which failed prematurely by shearing. The failure is attributed to the use of a free-cutting steel with a phosphorus content higher than that of recent specifications. Routine analytical control for such components is recommended.—G. F.

The Mechanism of Fatigue. P. L. Teed. (*Aeroplane*, 1952, **82**, Apr., 484–487). The current ideas of the mechanism of fatigue are reviewed from the purely metallurgical aspect. Some experimental results are discussed.—B. G. B.

The Alternating Bend Strength of Nitrided Specimens of Different Diameters. K. Wellinger and P. Gimmel. (*Arch. Eisenhüttenwesen*, 1952, **23**, May–June, 203–205). Alternating bend tests have been carried out on nitrided specimens 5·0, 6·5, and 8·0 mm. in dia. The results indicate that the effect of the nitrided layer would be small on specimens over 20 mm. in dia. and that, up to this diameter, the influence of surface finish is eliminated by nitriding. The starting point of the fatigue fracture lay, in general, just under the nitrided layer, though when stressed above the limit the fracture started in the outer fibres. Since in practice it is impossible to avoid occasional high stresses, this is probably the reason why service failures of nitrided steel do not exhibit the same type of fracture as those submitted to the fatigue test.—J. P.

Problems of Metallic Fatigue at High Temperatures. T. J. Dolan. (*Metal Progress*, 1952, **61**, Apr., 97–104). A review is given of work already carried out on the fatigue and creep of metals operating at high temperatures. The metallurgical treatment for optimum fatigue properties may be different from that for optimum creep properties, so a compromise must be reached.—B. G. B.

A Study of Fatigue in Metals by Means of X-Ray Strain Measurements. J. A. Bennett. (*Proc. Soc. Exper. Stress Anal.*, 1952, **9**, 2, 105–112). Specimens of SAE 4130 steel were fatigued by different amounts, after which static bending loads were applied and the resulting lattice strains measured by an accurate X-ray diffraction technique. The results showed that there was no correlation between this lattice strain and the amount of previous fatigue damage. This contradicts similar work by Glocker.—P. M. C.

Prolonged Hardness Testing. Part 1, M. V. Zakharov, M. V. Mal'tsev, A. A. Vashchenko, Z. A. Sviderskaya, and G. P. Lazarev; Part 2, S. M. Voronov and M. E. Rabinovich; Part 3, A. M. Korol'kov; Part 4, K. I. Portnoi and N. M. Sadchikova; Part 5, A. P. Gulyaev. (*Zavodskaya Laboratoriya*, 1950, No. 1, 78–93). [In Russian]. This is a symposium on the method of prolonged hardness testing for making a relatively quick evaluation of the influence of composition and structure of both ferrous and non-ferrous alloys on their high-temperature stability, with particular reference to creep. The method is based on ball indentation with the load applied for about 1 hr.—S. K.

'Elastic' and 'Plastic' Hardness of Hard Materials. P. Grodzinski. (*Nature*, 1952, **169**, May 31, 925–926). An evaporated silver layer enables the area of contact at maximum indentation pressure to be determined. Values of plastic and total hardness are given for boron and silicon carbide, synthetic corundum, and diamond.—A. G.

Internal Friction Measurements on Iron Wires of Commercial Purity. Å. Josefsson and E. Kula. (*Trans. Amer. Inst. Min. Met. Eng.: J. Met.*, 1952, **4**, Feb., 161–165). The authors have applied the internal friction method of determining carbon and nitrogen in solid solution in α-iron to commercial iron wires. The presence of impurities is found to broaden the internal friction curves for both elements, and also to change the position of the nitrogen curve. Ageing curves after various treatments are given, and the fixation of nitrogen by aluminium is illustrated. The inclination of nitrogen to stay in supersaturated solid solution, even at rather slow rates of cooling, is also shown.—G. F.

Contribution to the Study of the Ageing of Iron–Carbon Alloys by the Measurement of Electrical Conductivity. H. J. Seemann and W. Dickenscheid. (*Rev. Mét.*, 1952, **49**, May, 379–384). Specimens were maintained at temperatures between 50° and 100° C. and their resistance measured potentiometrically. Ageing was accompanied by a decrease in resistance. The variation of resistance with carbon content was determined and found to be linear.—A. G.

Influence of Thermal Treatment on the Rational Tensile Curves of Mild Steel. P. Bastien and C. Winter. (*Rev. Mét.*, 1952, **49**, May, 328–338). Three very mild steels of different known treatments were tested in the normalized (950° C.) annealed (850° C.) and overheated (975° C.) states and also after ageing. For a range of thermal treatments, the rational tensile curves show that the modulus of plasticity is considerably modified by ageing. Coefficients calculated from Kaiser-Wilhelm-Institut für Eisenforschung stamping test enabled the three steels to be classified after identical thermal treatments.—A. G.

Survey of the International Symposium on Abrasion and Wear. H. C. J. de Decker. (*Engineering*, 1952, **173**, May 30, 699–700). A critical review is given of the papers presented at the International Symposium on Abrasion and Wear, at Delft, Holland, on November 14 and 15, 1951.—M. D. J. B.

Measuring the Instantaneous Values of Induction and Field Strength and Demagnetization Losses of Transformer Sheets. W. Krug. (*Arch. Eisenhüttenwesen*, 1952, **23**, May–June, 207–215). The principles, development, and use of the Ferrometer apparatus for the measurement of instantaneous values of induction and field strength and of demagnetization losses in alternating magnetic fields are described. The apparatus can be also used for carrying out phase-dependent measurements of alternating current and voltage as well as for determining performance data.—J. P.

The Magnetic and Electrical Properties of Ferroxcube Materials. J. J. Went and E. W. Gorter. (*Philips Tech. Rev.*, 1952, **13**, Jan., 181–193). Ferroxcube materials are ferromagnetic oxidic materials specially developed for use at high frequencies. They belong to the group of ferrites crystallizing with the spinel structure. Owing to their high resistivity eddy-current losses do not as a rule occur. Residual losses, given by the expression $\tan \delta/\mu'$, may under certain conditions be considerable. The behaviour of μ' and $\tan \delta$ as a function of frequency is discussed. The conclusion is drawn that the magnetization process in ferrites differs from that taking place in metallic ferromagnetic materials. Attention is drawn to the remarkable dielectric properties of ferrites.—R. A. R.

Method for the Determination of Hysteresis Loop Area. W. B. Conover. (*Trans. Amer. Inst. Elect. Eng.*, 1951, **70**, Part II, 1485–1486). A method of finding the area of a hysteresis loop is derived, and shown to be more rapid than the planimeter. It is applied directly to loops on cathode-ray tubes but is limited to symmetrical and closed curves.—H. D. W.

Stainless Steel Magnetic Recording Wire. W. Sucksmith. (*Brit. Elect. Allied Ind. Res. Assoc., Tech. Rep.* N/T61, 1952, 1–6). The magnetic properties of stainless steel recording wire are investigated in the light of the theories of Stoner and Wohlfarth and of Néel on the shape anisotropy mechanism of hysteresis in heterogeneous magnetic alloys. The variation of coercive force and J_{REM}/J_{SAT} with time of heat-treatment at 450° C., 500° C., and 550° C. was required.—T. E. D.

A Recording Fluxmeter of High Accuracy and Sensitivity. P. P. Cioffi. (*Bell Lab. Record*, 1952, **30**, June, 247–251).

Magnetic Nickel–Iron Alloy. (*Engineering*, 1952, **173**, May 30, 691; *Engineer*, 1952, **193**, June 6, 775). **New Magnetic Material.** (*Elect. Rev.*, 1952, **150**, June 6, 1246). A brief description is given of a new material recently added to the range of magnetic alloys made by Standard Telephones and Cables, Ltd., Connaught House, Aldwych, London, W.C.2. The material known as ' Permalloy F,' a nickel–iron alloy, has a very rectangular hysteresis loop and a low value of coercive force obtained by the method of domain orientation.

Results Obtained by Ultrasonic Testing of Heavy Forgings Using the Impulse Reflection Method. R. Schinn and U. Wolff. (*Stahl u. Eisen*, 1952, **72**, June 5, 695–702). Tests with the ultrasonic reflection method have been carried out on forgings awaiting acceptance, in the course of manufacture, after rejection, and after long service. The recognition and location of defects were surprisingly accurate but the differentiation between dangerous and harmless flaws was not always possible. The size, direction, and position of flaws can be estimated from geometrical considerations. A statistical evaluation of test results from a thoroughly examined reject helped in interpreting the connection between type of defect and its cause. Ultrasonic testing, as used by various firms for acceptance testing of forgings, is described.—J. P.

Boiler Drum Flaws: Ultrasonic Detection Practice. C. J. Richards. (*Elect. Rev.*, 1952, **150**, Apr., 861–864). A description is given of the ultrasonic method of flaw detection in boiler drums. The interpretation of the reflection patterns is discussed and it is shown that certain cracks near rivet holes are undetectable. A combination of ultrasonics and magnetic methods results in a comprehensive drum inspection.—H. D. W.

Crack Detection by Fluorescent Ink. (*Engineering*, 1952, 173, June 6, 724). **Fluorescent Ink for Crack Detection.** (*Chem. Age*, 1952, **66**, June 14, 899–900). The use of ' Glo-mor ' fluorescent ink, developed by the Manchester Oil Refinery, for the simple and very rapid method of detecting surface cracks is described. The ink penetrates cracks which then fluoresce brightly in ultraviolet light. It may be used on light alloys, plastics, or steels.—T. E. D.

Non-Destructive Testing Team, E.C.A.—Radio-Active Isotopes and Their Uses. L. Van Ouwerkerk. (*Metalen*, 1952, **7**, Feb. 15, 41–44). [In Dutch]. The author reports what a Dutch team saw and learned in the U.S.A. in connection with isotopes. The report is divided as follows: (1) The nature of isotopes and neutron bombardment; (2) the atomic pile; (3) the preparation and supply of isotopes; (4) methods of registering radiation; (5) some applications; and (6) methods of protection.—R. S.

Approximations in Transient Surface Heating. J. C. Jaeger. (*Australian J. Sci. Res.*, 1952, **5**, Mar., 1–9). A number of exact mathematical solutions for the surface temperature of a semi-infinite solid, which is heated over a region of its surface, are given. These have applications in surface induction heating, X-ray tube anode heating, and heating by friction. Temperatures within the solid are discussed.—T. E. D.

The Creep of Metals and Methods of Measurement. E. Morlet. (*Métaux–Corrosion–Indust.*, 1952, **27**, Mar., 107–118). After considering various types of metallic deformation, the author presents a theoretical discussion concerning the basic principles of creep. Different ways of graphical presentation of the results and five methods of extrapolation of the graphs are discussed. The influence of mechanical treatment, thermal treatment, and crystalline structure on creep resistance is reviewed.—B. G. B.

Creep Under Complex Stress Systems at High Temperatures. A. E. Johnson. (Inst. Mech. Eng.: *Aircraft Eng.*, 1952, **24**, Jan., 6–16). The results of simple torsion and torsion creep tests, by addition or subtraction of suitable hydrostatic stresses, can be used to study general stress systems. Experimental confirmation is given. The general method is used to study the high-temperature creep properties of Nimonic 75, at 550° C. and 650° C., and 0·17% carbon steel at 350° C. 450° C., and 550° C. A mathematical treatment is included.

Creep Strength and its Connection with other Mechanical Properties from a Physical Point of View. A. Kochendörfer. (*Arch. Eisenhüttenwesen*, 1952, **23**, May–June, 183–191). The deformation and fracture of single crystal and polycrystalline metals involving slip and the influence of dislocations on these phenomena are discussed.—J. P.

A Constant Stress Device Adjustable for Specimen Length. A. J. Kennedy. (*J. Sci. Instruments*, 1952, **29**, Feb., 40–44). By the addition of a secondary profile, it is shown that the Andrade-Chalmers profile device can be made applicable to specimens (in creep tests) ranging in length from 0·5 to 1·5 times the lengths for which the main profile was designed.

Influence of Structure on the Plastic Flow of Steel above the A_3 Point. P. Feltham. (*Nature*, 1952, **169**, June 7, 976). The possibility of deducing the flow rate/temperature relation for a given stress from a single experiment under conditions of increasing temperature was studied. The influence of initial flow state precludes this, probably because of non-attainment of characteristic grain size at all temperatures.—A. G.

How Can We Appraise Metals for High Temperature Service ? T. J. Dolan. (*Metal Progress*, 1952, **61**, Mar., 55–60). Interaction between time-dependent creep and cycle-dependent fatigue results in a rate of progressive damage which may be quite different from that predicted from knowledge of effects obtained in separate tests. The following factors must be given careful consideration: (*a*) Frequency of repetition of the stress cycle; (*b*) metallurgical instability of most metals at high temperatures; (*c*) influence of surface effects (*i.e.*, erosion, oxidation or corrosion); (*d*) accentuation of time-dependent creep caused by imposing a mean or steady stress; and (*e*) the pronounced reduction of fatigue strength caused by stress raisers.—B. G. B.

METALLOGRAPHY

The Measurement of Unresolved, Single Particles of Uniform Thickness by Means of Variable Phase Microscopy. H. Osterberg and G. E. Pride. (*J. Opt. Soc. Amer.*, 1950, **40**, Feb., 64–73). It is shown that with a variable phase microscope, the centre of the sharply focused diffraction image of a single unresolved particle can be brought to zero intensity by a definite choice of the parameters, specifically the phase difference δ between, and the amplitude transmission ratio h of, the conjugate and complementary areas of the diffraction plates.—H. D. W.

The Insides of Metals. C. A. Zapffe. (*Physics Today*, 1950, **3**, Sept., 13–19). The microscopic examination of fractured metals by a special ' fractographic ' technique is described; this allows the study of the structure of individual fractured grains. Examples of the application of this technique to a number of metallurgical problems are given.—B. G. B.

Resolving Power in Diffraction Microscopy with Special Reference to X-Rays. A. V. Baez. (*Nature*, 1952, **169**, June, **7**, pp. 963–964). A resolution criterion is derived and two limiting cases considered: (1) A point source and a film of given resolving power; and (2) a grainless film and illuminating source of given width. A method of producing a useful single-zone plate for X-rays is suggested.—A. G.

A Specimen-Treating Adaptor for the Electron Microscope. N. Sasaki and R. Ueda. (*Rev. Sci. Instruments*, 1952, **23**, Mar., 136–138). The design of a hard glass tube adapter is given which enables a substance already placed on the specimen holder to be subjected to certain physical or chemical treatment and then moved to the position of observation without exposing it to the air.—H. D. W.

Electron Microscopical Examination of Metal Surfaces. V. C. Dalitz and J. A. Schuchmann. (*Metalen*, 1952, **7**, May 15, 153–161). [In Dutch]. The preparation of replicas is dealt with. The etched sample is placed in a high vacuum bell jar. A layer of silver about 5μ thick is produced by evaporation from a small crucible. The negative replica is coated with silicon monoxide or carbon. The silver is dissolved in nitric acid and the shadow treatment applied.—R. S.

The Electronic Microscope and Metallography. A. Septier and M. Gauzit. (*Mét. Constr. Méc.*, 1952, **84**, Mar., 165–171; Apr., 241–249). The principles of high resolving powers and wave mechanics are reviewed. The various types of electronic lenses, faults in lenses, the source of electrons, the optical system, quality of images, and the printing technique are discussed. Emission, reflection, and transmission methods are described. The application of the electron microscope to the study of surface finish, corrosion phenomena, precipitation hardening, and mechanical and heat-treatments is examined.

New Studies, by Electron Diffraction, on the Cementation of Iron. J. J. Trillat and S. Oketani. (*Rev. Mét.*, 1952, **49**, Apr., 262–266). Polycrystalline films of iron prepared by vacuum sputtering were studied under the action of pure carbon monoxide and a 50/50 mixture of carbon monoxide and hydrogen. Pure CO gives, with rising temperature, oxide, cementite, and graphite. With hydrogen present, no oxide is formed and a phase given the formula Fe_2C is observed which is unstable above 400° C. Single crystals of iron were treated with the above gas mixture and the orientation of cementite in relation to iron was studied.—A. G.

On the Focusing Conditions for Double-Crystal X-Ray Spectrometers. R. C. Spencer. (*Rev. Sci. Instruments*, 1952, **23**, Mar., 138–139). The desirability of placing a Geiger counter at a focal point moving with the second crystal, or at a focal point fixed in space is discussed in relation to small-angle scattering from a sample which is illuminated by monochromatic X-rays.—H. D. W.

X-Ray Diffraction: Raymax Crystallographic Equipment in Steel Research. T. K. Jones. (*Metro. Vick. Gaz.*, 1952, **24**, Apr., 155–158). A description of the Raymax X-ray set is given. The application of the Laue method in industry is limited by the need for large crystal specimens, but a special camera enables a sample in the form of powder to be investigated.—H. D. W.

X-Ray Studies of Metal Structures. W. H. Taylor. (*Brit. Elect. Allied Ind. Res. Assoc., Tech. Rep.* N/T62, 1952, 1–12). The application of an X-ray microbeam technique to the study of metal textures is described, and the examination of modulated structures in certain Cu–Fe–Ni alloys is outlined. Phase structures of alloys of aluminium with transition metals, of sigma-phase alloys, and of interstitial materials have been studied.—T. E. D.

A Monitor for Measuring X-ray Beam Intensities. C. M. Hargreaves, E. Prince, and W. A. Wooster. (*J. Sci. Instruments*, 1952, **29**, Mar., 82–85). The design of a monitor is given in which the incident X-rays produce ionization in a short length of their normal path, and the ions thus produced are used to measure the intensity of the X-rays at the same time as they are being diffracted. The accuracy of the measurement is about 1%.—H. D. W.

A Novel High Vacuum Furnace. E. J. Caule. (*Canad. J. Techn.*, 1952, **30**, Apr., 63–65). Radiant heat leaving a Globar heater at the focus of a parabolic mirror is projected on to another parabolic mirror, and is there focused on the metallic specimen. Details of the construction, operation, and performance of the apparatus are given.—B. G. B.

Origin of Recrystallization Textures. J. E. Burke. (*Trans. Amer. Inst. Min. Met. Eng.: J. Met.*, 1952, **4**, Mar., 263–264). In considering the origin of recrystallization textures in metals, the author outlines the hypotheses of orientated nucleation and of orientated growth, and discusses the possibility of these two mechanisms operating together.—G. F.

Nucleation Period of the Austenite Transformation for Different Types of Cooling. F. Wever and O. Krisement. (*Arch. Eisenhüttenwesen*, 1952, **23**, May–June, 229–237). The 'continuous' transformation curve for a given steady rate of cooling can be calculated from the isothermal transformation diagram and *vice versa*, and the requisite formulæ for linear and Newton's law of cooling have been derived. Although an isothermal diagram calculated from the continuous transformation curve of a particular steel did not agree with the experimental isothermal curve, this was due to unavoidable errors in performing the isothermal transformation. When calculated corrections were applied, agreement was good. On account of the difficulties of ensuring ideal cooling conditions in plotting isothermal transformation curves, it is suggested that continuous diagrams be used and the ideal isothermal curves calculated therefrom.—J. P.

Growth of Austenite in Cold-Rolled Tempered Martensite. A. E. Nehrenberg. (*Trans. Amer. Inst. Min. Met. Eng.: J. Met.*, 1952, **4**, Feb., 181). The author shows that the growth, which is equiaxed rather than acicular, of austenite in cold-worked tempered martensite reflects the matrix grain structure existing at the time of austenite formation, and therefore there is no contradiction of the principle that the shape assumed by growing austenite is inherited from the previous structure.—G. F.

Effect of Applied Stress on the Martensitic Transformation. S. A. Kulin, M. Cohen, and B. L. Averbach. (*Trans. Amer. Inst. Min. Met. Eng.: J. Met.*, 1952, **4**, June, 661–668). The formation of martensite in a stressed 20% Ni, 0·5% C steel has been studied as a function of the temperature of loading and the state of stress. The transformation can be initiated by elastic stresses at temperatures above that at which martensite is normally formed. Shear strains and normal tensile strains acting on a potential habit plane promote the transformation, but compressive strains oppose it. A combined strain criterion is presented, which takes into account the shear and dilatation components of the atomic displacements during the transformation.—G. F.

Isothermal Martensite Formation in an Iron–Chromium–Nickel Alloy. S. A. Kulin and G. R. Speich. (*Trans. Amer. Inst. Min. Met. Eng.: J. Met.*, 1952, **4**, Mar., 258–263). The isothermal formation of martensite at sub-zero temperatures has been studied in a 14% Cr, 9% Ni austenitic stainless steel. The amount of martensite formed isothermally in a given time is found to follow a '**C**-curve' behaviour with decreasing temperature, and the temperature at which the transformation begins is dependent on the cooling rate. It is believed that a general martensite transformation theory must include not only the basic concepts of the strain embryo theory, but also the rôle of thermal fluctuations.—G. F.

Relation between the Volume of Martensite and the Number of Martensitic Plates per Unit Volume. E. S. Machlin. (*Trans. Amer. Inst. Min. Met. Eng.: J. Met.*, 1952, **4**, Mar., 277–278). Assuming a constant thickness for martensitic plates, which appears to be consistent with experimental data, the author derives a relationship between the volume fraction of martensite and the number of martensitic plates per unit volume. The basis of the application of lineal analysis for measuring the volume fraction of martensitic plates is re-established.—G. F.

The Iron–Phosphorus–Manganese System. R. Vogel and J. Berak. (*Arch. Eisenhüttenwesen*, 1952, **23**, May–June, 217–223). The phase diagram of the $Fe–Fe_2P–Mn_2P–Mn$ field of the Fe–P–Mn system has been derived from thermal and microscopic examinations. Fe_2P and Mn_2P form a continuous range of solid solutions with a flat melting point maximum at 39·4% Fe, 38·8% Mn, 21·8% P. A second range of solid solutions between Fe_3P and Mn_3P shows a region of instability between 20% and 57% Mn, in which the $Fe_3P–Mn_3P$ solutions are replaced by those of $Fe_2P–Mn_2P$ in equilibrium with Fe–Mn solid solutions. The affinities of iron and manganese for phosphorus are not very different.

Silicon–Oxygen Equilibrium in Liquid Iron. N. A. Gokcen and J. Chipman. (*Trans. Amer. Inst. Min. Met. Eng.: J. Met.*, 1952, **4**, Feb., 171–181). The authors have studied the rates of oxidation and reduction in Fe–Si–O alloys melted in silica crucibles, brought into equilibrium with atmospheres of controlled H_2O/H_2 ratio, and quenched in hydrogen. The transfer of oxygen between gas and metal is found to determine the rate of the process. From a detailed study of equilibrium conditions, the activity coefficient of oxygen is found to be strongly diminished by even small amounts of silicon. The concentration product $[\%\ Si][\%\ O]^2$ is constant, for practical purposes, in the range 0·02 to 15·0% Si.—G. F.

Three-Dimensional Representation of the Free Energy in Ternary Systems. E. Rudberg. (*Jernkontorets Ann.*, 1952, **136**, May, 91–112). [In Swedish]. General properties of the Gibbs' free-energy function for ternary systems are discussed in relation to the geometrical representation as a series of single-phase curved surfaces. Hultgren's ortho- and para-equilibria, chemical potential and activity, and Hillert's isoactivity lines are treated.—G. G. K.

CORROSION

Stress Corrosion: Its Relation to Other Types of Corrosion. U. R. Evans. (*Corrosion*, 1951, **7**, July, 238–244). Corrosion damage is discussed under the headings of unlocalized corrosion, pitting, intergranular, and stress corrosion and corrosion fatigue, the last two being the most damaging. Stress corrosion is shown to occur on pre-existing weak points caused

by certain heat-treatments. It may be avoided by annealing or by producing compressive stresses.—E. C.

Definitions and Mechanism of Passivity. R. Piontelli. (*Werkstoffe u. Korrosion*, 1952, **3**, May–June, 188–192). This is an abridged version of the author's views on these subjects, as expounded to the 3rd (Berne, 1951) Congress of the Comité International de Thermodynamique et de Cinétique Electrochimiques. Broadly, he accepts the definition of H. H. Uhlig and R. B. Mears, *viz.*: "A metal or alloy is passive if it substantially resists corrosion in an environment where thermodynamically there is a large free-energy decrease associated with its passage from the metallic state to appropriate corrosion products." He examines briefly the effects on passivity of individual factors, such as surface films and the characteristics of the corrosive solution.—J. C. H.

Effect of Wetting Agents on Corrosion. III—Influence of the Constitution of Wetting Agents with Active Cations. L. Piatti. (*Werkstoffe u. Korrosion*, 1952, **3**, May–June, 186–188). Further results are given of laboratory tests on mild steel immersed in water containing 0·4% of wetting agents. A curious difference was observed between the effects of two different derivatives of the same mono-acetylated diamine. One of them, the hydrochloride, practically stopped corrosion, but the other, the methylammoniumsulphomethylate, gave rise to appreciably greater attack, of the pitting type, than water alone. Thus, although ionizable wetting agents can be used as inhibitors it is important to select one with the right constitution; otherwise more harm may be done than good.—J. C. H.

The Formation of Surface Layers and Protective Films. L. W. Haase. (*Werkstoffe u. Korrosion*, 1952, **3**, May–June, 198–205). A distinction is drawn between surface layers, which merely cover a metal, and protective films, which protect it from corrosion. The natural formation of both types of layer or film is discussed at some length for a variety of ferrous and non-ferrous metals when exposed to corrosion by various waters.—J. C. H.

Possibilities and Limitations of Cathodic Protection. C. Wagner. (*Werkstoffe u. Korrosion*, 1952, **3**, May–June, 172–176). This purely theoretical paper presents the essential conclusions drawn by the author from his more detailed studies of the subject (*J. Electrochem. Soc.*, 1952, **99**, 1). Cathodic protection will only occur when the potential gradient across the corrosive solution between the metal to be protected and the sacrificial anode lies below a limiting value at every point. This potential gradient is calculated for typical geometrical assemblies, *e.g.*, for two concentric cylinders of different metals.—J. C. H.

Corrosion Problems: Past, Present and Future. U. R. Evans. (*Werkstoffe u. Korrosion*, 1952, **3**, May–June, 165–172). This is a German translation of the author's inaugural address to the Corrosion Group of the Society of Chemical Industry which was formed in 1951. His main purpose is to stimulate more general interest in corrosion as a subject and to induce younger members of the Society to take up its study.—J. C. H.

Chemical Causes of Wear. J. J. Broeze. (*Engineering*, 1952, **173**, May 30, 693–694). The author distinguishes between mechanical, physical, and chemical forms of wear, and then considers in detail three types of chemical wear: (1) Chemical attack by the atmosphere; (2) chemical attack by lubricants; and (3) chemical attack in an engine by the combustion products. (11 references).—M. D. J. B.

How to Predict Damage from Corrosion in Hot-Water Installations. G. Seelmeyer. (*Werkstoffe u. Korrosion*, 1952, **3**, May–June, 211–217). This paper is based on the results of a systematic survey of large hot-water installations in Germany from 1939 to 1945. Particulars of water composition, type of plant, and degree of corrosion are given for 46 installations. The observations are classed under seven heads according to the characteristics of the water, *e.g.*, non-aggressive and non-scale-forming, non-aggressive but scale-forming, aggressive, etc. The main conclusion is that the occurrence of corrosion in hot-water installations can never be ascribed to a single factor, such as the oxygen content of the water. Moreover, the chemical composition of the water is not alone of importance; the type of material used in the plant, the method of fabrication, and the operating conditions are also significant. Thus, hot-water installations should always be worked well within their capacity, with suitable rest periods for individual boilers; plants working within their capacity suffer much less corrosion than overloaded plants. The characteristics that render water aggressive or non-aggressive, scale-forming or non-scale-forming are summarized.—J. C. H.

The Rôle of Polarization in Electrochemical Corrosion. R. H. Brown, G. C. English, and R. D. Williams. (*Corrosion*, 1950, **6**, June, 186–195). Electrochemical polarization is the term covering all the factors involved in the irreversible electrode phenomena associated with electrochemical corrosion. The paper deals with polarization diagrams, correlation of polarization with corrosion data, the correct proportions of the total polarization ascribed to each electrode reaction and its implications, and other factors.—H. F.

Trends in Corrosion Control. H. H. Uhlig. (*Metal Progress*, 1952, **6**, Mar., 82–88). A number of trends are discussed including the wider applications of cathodic protection, more general application of corrosion-resistant alloys and metals, increased use of protective metal coatings, greater use of inhibitors, broader use of paints, ceramics, and plastics, and increased employment of corrosion engineers by industry.

Effect of Specimen Shape on Corrosion in the Atmosphere. H. R. Copson. (*Corrosion*, 1951, **7**, Oct., 335–338). The corrosion rate of metals in the atmosphere depends on the amount of contamination actually coming into contact with the specimen. One of the variables which determines this is its shape. Several illustrations of plain and threaded bars, wire, and sheet specimens, and insect screen are given.—H. F.

A Polarographic Method for the Continuous Determination of the Consumption of Oxygen in Corrosion Tests. P. Delahay. (*Corrosion*, 1951, **7**, May, 146–150). A description is given of laboratory experiments and apparatus for determining the rate of oxygen consumption in corrosion tests carried out in distilled water or in mixtures of two dissolved gases. The method may prove specially suitable for the study of the formation of films and coatings on metallic surfaces.—E. C.

Surface Areas of Metals and Metal Compounds: A Rapid Method of Determination. C. Orr, jun., H. G. Blocker, and Susan L. Craig. (*Trans. Amer. Inst. Min. Met. Eng.: J. Met.*, 1952, **4**, June, 657–660). The determination of surface area is of importance in corrosion research. The authors present a simplified technique, involving liquid-phase adsorption, for the determination of the metal surface area, which is suitable for routine work. The investigation described, which considerably extends previous work, shows that fatty acid adsorption can be used to determine the surface area of a variety of metals and metal compounds, and offers further confirmation of the correctness of gas adsorption methods.

Protection of Electrical Equipment against Corrosion in Industrial Plants. H. E. Springer. (*Trans. Amer. Inst. Elect. Eng.*, 1951, **70**, Part II, 1605–1612). Common instances are given of the corrosion of electrical equipment by acids, alkalis, salts, brines, high temperature, and moisture. Practical methods of protection are considered for heavy power apparatus, control equipment, rotating apparatus, and wiring systems.—H. D. W.

The Prevention of Corrosion in Packaging. I—The Use of Sodium Benzoate as a Corrosion-Inhibitor in Wrappings. II—Temporary Protectives from Rubber Latex and Other Aqueous Dispersions. III—Vapour-Phase Inhibitors. E. G. Stroud and W. H. J. Vernon. (*J. Appl. Chem.*, 1952, **2**, Apr., 166–184).

Corrosion in Sulfur Production: The Use of an Alloy Protective Coating. N. Hackerman and D. A. Shock. (*Corrosion*, 1950, **6**, June, 195–200). Field and laboratory tests have measured the rates of corrosion during the various phases of sulphur production. Severe corrosion occurred only when water and molten sulphur were in contact with the steel surfaces together. An alloy coating containing zinc, magnesium, and aluminium is effective in resisting corrosion, especially with a high concentration of the light metals.—H. F.

Influence of Sulphate-Reducing Bacteria on the Corrosion Potential of Iron. J. N. Wanklyn and C. J. P. Spruit. (*Nature*, 1952, **169**, May 31, 928–929). Results from experiments in which sterile media were inoculated with bacteria, after the attainment of steady potentials, are given. Bacterial stimulation of corrosion under heterotrophic conditions is ascribed to the action of sulphide as an anodic stimulator.—A. G.

Corrosion Studies of Iron in the Presence of Sulfur. R. B. Seymour, W. R. Pascoe, and R. D. Stout. (*Corrosion*, 1951, **7**, Aug., 265–268). The composition and properties of sulphur cements are discussed with a view to reducing corrosion at iron–sulphur interfaces in the presence of water. Partially

immersed iron rods suffered corrosion which was accelerated by addition of sodium chloride to the cement or surrounding water. No corrosion occurred on completely immersed rods during two years, even with very thin sulphur coatings.—E. C.

Materials of Construction for Handling Sulfuric Acid. TP-5A—Materials, Handling, and Manufacturing Sulfuric Acid. S. W. Shepard. (*Corrosion*, 1951, **7**, Aug., 279–282). Metallic and non-metallic materials which reduce or eliminate sulphuric acid corrosion are listed and discussed. Recently developed organic compounds proved particularly suitable and practically inert over wide ranges of concentration.—E. C.

Influence of the Composition of Rust-Resisting Steels on Their Resistance to Sulphuric Acid and Hydrochloric Acid. K. Bungardt and H. J. Rocha. (*Werkstoffe u. Korrosion*, 1952, **3**, May–June, 209–211). The principles to be observed when choosing rust-resisting steels to withstand acid attack are illustrated by reference to solid models showing the results of laboratory tests on typical rust-resisting steels and nickel alloys in various acids. The co-ordinates are acid concentration, temperature, and loss in weight. Materials of this type may be classed in two groups: (1) Steels that become truly passive in certain acids because of their chromium content; and (2) steels resembling nickel alloys, which remain active but are characterized by a low corrosion rate. In practice, satisfactory results may be obtained from steels of either group but, if a steel of the second group is used, its corrosion rate should not exceed 0·1 g./sq. m./hr. (0·4 mil per year). Rust-resisting steels cannot become passive in HCl but all steels containing more than 13% Cr do so in HNO_3. In other acids, of which H_2SO_4 is typical, there are well-defined fields of passivity or activity. The effects of secondary factors may differ according to the state of the steel. Thus, oxygen in solution will increase the loss in weight of a steel in the active condition but will help to maintain passivity if the steel is in the passive condition.—J. C. H.

The Sensitivity of Electrochemical Determinations of Corrosion Reactions and Oxygen Contents. F. Tödt. (*Werkstoffe u. Korrosion*, 1952, **3**, May–June, 205–209). The author reviews his work on methods for determining the thickness of oxide films on iron and the oxygen content of water. These are based on colorimetric determinations of the iron passing into solution in the early stages of corrosion, or on measurements of the current yielded by a cell in which the cathodic reaction is controlled by oxygen depolarization. The latter method is particularly valuable for the determination of low concentrations of oxygen, such as are present in feed water for high-pressure boilers. By its use an accuracy of 0·001% oxygen can be achieved.—J. C. H.

Some Applications of Organic Corrosion Inhibitors in the Petroleum Industry. C. M. Blair, jun. (*Corrosion*, 1951, **7**, June, 189–195). Methods and apparatus for applying organic inhibitors are described and the mechanism of the preventive action of the latter is discussed. Laboratory and field data are compared with predictions based on the adsorption theory.—E. C.

Stainless Steels for the Bottoms of Tanks containing Sour Crude Oil. C. P. Larrabee, and W. F. Rogers. (*Corrosion*, 1951, **7**, Aug., 276–278). Laboratory experiments are described indicating that austenitic stainless steels resist corrosion which normally occurs through the action of sulphide-bearing salt water when plain carbon steels are used.—E. C.

Corrosion in Condensate and in High Pressure Sweet Oil Wells. R. C. Buchan. (*Corrosion*, 1950, **6**, June, 178–185). The problem of corrosion in high-pressure sweet oil wells is compared with the similar better-known problem in condensate wells, and both problems are discussed. The mechanical aspects of, and chemicals used for, corrosion protection are given, and a summary is made of methods of determining the corrosive conditions and the efficiency of methods to prevent corrosion.—H. F.

High Pressure Sweet Oil Well Corrosion. H. L. Bilhartz. (*Corrosion*, 1951, **7**, Aug., 256–264). Different types of tube pitting and scaling are discussed and known remedies are shown to be ineffective. Coating of tubes with thermo-setting plastics generally proved successful. The use of nickel-alloyed and nickel-plated steels is suggested.—E. C.

Corrosion on New Distillation Unit Processing Low Sulfur Crude. D. L. Burns. (*Corrosion*, 1950, **6**, June, 169–177). The author reports the history of corrosion experienced on a new atmospheric and vacuum crude-oil distillation unit. The crude oil charged was a low-sulphur type averaging 0·20% sulphur. A comparison with the operating conditions on an earlier, similar unit, which did not experience corrosion, is made.—H. F.

Literature Survey on Corrosion in Neutral Unaerated Oil Well Brines. H. R. Copson. (*Corrosion*, 1951, **7**, Apr., 123–126). A survey was carried out to elucidate the problem of oil-well subsurface corrosion, and the literature is discussed with particular reference to hydrogen evolution and depolarization with oxygen or other agents. Other factors dealt with in connection with corrosion in neutral unaerated brines are wetting capacity of oil, erosive, stress, and fatigue effects, and the use of inhibitors. (46 references).—E. C.

A Proposed Standard Method for Measuring the Electrical Resistance of Pipe Line Coatings. W. F. Rogers, B. H. Davis, L. Sheppard, L. G. Sharpe, E. R. Allen, D. Bond, and P. T. Miller. (*Corrosion*, 1951, **7**, July, 245–251). Apparatus and methods are described for use *in situ* to determine the electrical resistance of pipeline coatings. Necessary precautions are specified and instructions are given for calculating and reporting data.—E. C.

Corrosion of Metals by Insecticidal Solutions. G. S. Cook and N. Dickinson. (*Corrosion*, 1950, **6**, May, 137–139). The authors report an investigation to determine the comparative corrosion resistance of FS-1α magnesium alloy, 24-ST aluminium alloy, aluminium, yellow brass, copper, cold-rolled steel, galvanized steel, and 18/8 stainless steel, when immersed in various insecticide solutions. The stainless steel was found to be the only completely corrosion-resistant metal.—H. F.

Control of Corrosion Damage to Rolling Stock through Proper Design and Maintenance. F. K. Mitchell. (*Corrosion*, 1951, **7**, Aug., 269–275). Figures of corrosion damage of railway wagons and carriages in the U.S.A. are quoted. Improvements in design are discussed and proper selection of structural and maintenance materials is suggested to combat the more common causes of corrosion.—E. C.

Testing Inhibitors of Brine Drippings Corrosion of Railway Tracks and Equipment. G. M. Magee. (*Corrosion*, 1951, **7**, June, 186–188). The addition of dichromate to saturated salt solution was too expensive, but effective in preventing corrosion by refrigeration car drippings. Sodium Polyphos may be a cheaper equally effective alternative.—E. C.

Corrosion of Railroad Hopper Car Body Sheets. B. J. Kelly. (*Corrosion*, 1951, **7**, June, 196–201). The corrosion resistance of various steels was investigated and high-strength steels were found to be more corrosion-resistant than copper or carbon steels. Good design is essential for reducing corrosion.

Bacterial Casing Corrosion in the Ventura Field. K. Doig and A. Wachter. (*Corrosion*, 1951, **7**, July, 212–224). Causes for well-casing corrosion are examined and preventive measures are suggested. Bacterial action was found responsible for some types of corrosion but no economic preventive measures for existing wells could be devised. In future wells, operationally satisfactory high-*p*H drilling fluids would effectively inhibit or destroy the bacteria.—E. C.

Corrosion in the Ammonolysis of Aliphatic Chlorides. R. S. Treseder and R. F. Miller. (*Corrosion*, 1951, **7**, July, 225–228). Results of corrosion-resistance determinations of various alloys are presented. These had been carried out in connection with a process for manufacturing methallylamine by high-pressure liquid-phase ammonolysis of methallyl chloride. Anhydrous solution of ammonium chloride in liquid ammonia and a mixture containing methallylamine and unreacted aliphatic chloride were found to be most corrosive. These and other combinations are generally corrosive to carbon steel and common alloys at high temperatures.—E. C.

Prevention of Corrosion in Naval Aircraft. N. E. Promisel and G. S. Mustin. (*Corrosion*, 1951, **7**, Oct., 339–352). Corrosion prevention of aircraft starts with proper design, proper selection of materials, and control of fabrication processes. Free drainage, avoidance of dissimilar metal contacts, electrochemical insulation, proper heat-treatment, and cleanliness are stressed.—H. F.

Corrosion Problems Related to Air Transport Aircraft. O. E. Kirchner and F. M. Morris. (*Corrosion*, 1952, **7**, May, 161–177). The main causes of corrosion in aircraft are discussed. Special attention is paid to moisture-collecting areas, corrosion from exhaust gases, abrasion, the spilling of food, and water-containing chlorides and sulphates. Corrosion may be reduced to a minimum by good design and selection of metals, avoidance of dissimilar metal contacts, and various inhibitive and protective treatments.—E. C.

Porcelain Enamel as a Corrosion Resistant Coating for Metals. G. H. McIntyre. (*Corrosion*, 1951, **7**, Apr., 118–123). Enamelling processes and specifications are discussed with regard to a variety of uses for corrosion-resistant coatings. Resistance to impact damage is considered. Corrosion resistance is limited by non-resistance of silica or silicates to fluorides, hydrofluoric acid, and strong caustic solutions, but resistance can also be increased by high perfection of surface finish.—E. C.

Corrosion Resistant Equipment for the Corn Refining Industry. R. W. Flournoy. (*Corrosion*, 1951, **7**, Apr., 129–133). An outline is given of specialized techniques used to obtain corrosion data for corn refining processes. Corrosion rates and cost index values are tabulated, and the order of suitability of various materials is given.—E. C.

Resistance to Intergranular Corrosion of Ferritic and Martensitic Stainless Chromium Steels. E. Houdremont and W. Tofaute. (*Stahl u. Eisen*, 1952, **72**, May 8, 539–545). Stainless steels (with 15–30% Cr) and various additions of molybdenum, silicon, aluminium, phosphorus, titanium, tantalum, niobium, and vanadium have been tested, in the form of sheets 2–6 mm. thick after quenching from 700–1350° C., for susceptibility to intergranular corrosion in copper-sulphate/sulphuric-acid. It is concluded that: (1) Steels with 17% Cr and various carbon contents, after quenching from above 850° C., are susceptible to intergranular corrosion because these steels consist, at the quenching temperature, of a very low carbon, high-chromium ferrite, and a very high carbon (up to 0·4% C) austenite from which carbides precipitate on cooling to room temperature. (2) Resistance to intergranular corrosion is achieved in steels with only 0·01% carbon if, before quenching, they are decarburized in hydrogen and nitrided. (3) In 17% Cr steels, additions, which combine with carbon and nitrogen (also Ti, Ta, and Nb) and thus favour ferrite formation, eliminate the tendency to intergranular corrosion, in contrast to vanadium, if their ratio to carbon is greater than is theoretically required by the formula of the respective carbides. (4) Addition elements which have no great affinity for carbon (Si, Al, P, and Mo) have no effect on susceptibility to intergranular corrosion. (5) Since steels with 24% Cr and over exhibit the same susceptibility, the theory of chromium impoverishment as the cause of the corrosion must be modified. It is assumed that the precipitated material is iron carbide containing varying proportions of chromium and that these reactive carbides cause the intergranular corrosion. (6) Investigations show that carbide precipitation is also responsible for intergranular corrosion in martensitic stainless steels.—J. P.

Passivating Characteristics of the Stainless Steels. W. G. Renshaw and J. A. Ferree. (*Corrosion*, 1951, **7**, Oct., 353–360). Experimental work, by means of solution-potential measurements, for following the progress of film formation on stainless steels, indicates that the rate of film growth and degree of passivity vary considerably under different conditions. A comparison of several stainless steel types in regard to their passivating characteristics is made and the effects of surface finish are also considered. Results suggest that in some cases stainless steels probably owe their corrosion resistance to a protective film of corrosion product on the surface rather than to the passive film which, according to the usual concept, is caused by an oxidizing environment.—H. F.

Liquid Sodium—A Noncorrosive Coolant. R. F. Koenig and S. R. Vandenberg. (*Metal Progress*, 1952, **61**, Mar., 71–75). Owing to its high specific heat and low density, liquid sodium is an excellent coolant and has been investigated in connection with nuclear energy. Details are given of a number of tests to investigate the corrosive effect of liquid sodium; a number of stainless steels have been found suitable. B. G. B.

Filamentary Growths on Metal Surfaces—" Whiskers." K. G. Compton, A. Mendizza, and S. M. Arnold. (*Corrosion*, 1951, **7**, Oct., 327–334). Filamentary growths (of fibres up to $\frac{3}{4}$ in. long and 2μ in dia.) have been found on metal surfaces of some of the parts used in telecommunications equipment and are similar to those known as ' whiskers ' which developed between the leaves of cadmium-plated condensers. An extensive investigation is in progress and some results are reported. These ' whiskers ' are not compounds, but are metallic filaments in the form of single or twinned crystals, and they can grow on solid metals as well as on electrodeposited coatings. The growths may develop in an environment of relatively low humidity and in which there are, at most, only traces of organic material.—R. A. R.

Current Requirements for the Cathodic Protection of Steel in Dilute Aqueous Solutions. G. R. Kehn and E. J. Wilhelm. (*Corrosion*, 1951, **7**, May, 156–160). Colorimetric analysis was employed to determine experimentally the minimum current density necessary to protect steel cathodically against dilute salt-water corrosion. The minimum current densities are compared with those obtained from a Britton curve for each of the two test solutions of sodium sulphate and sodium chloride. The significance of the minimum current densities determined by the two methods is discussed.—E. C.

External Ship Corrosion Due to Bacterial Action. W. S. Patterson. (N.-E. Coast Inst. Engineers and Shipbuilders: *Shipbuilder and Marine Engine Builder*, 1952, **59**, Apr., 343–346). A ship lay for 11 weeks in river water which regularly altered in character every 12 hr. and, for a considerable proportion of the 12 hr., the ship rested on mud banks; these were highly infested with bacteria. The affected steel plates were generally outstrakes on the bottom of the ship; rivet points were severely corroded in the same areas. The mechanism of the corrosion is discussed in terms of the modern electrochemical principles of corrosion. The results of a number of corrosion tests carried out on the mud are given together with suggestions for the protection of the hull by a suitable paint.—B. G. B.

Cathodic Protection of an Active Ship in Sea Water—Part II. K. N. Barnard. (*Corrosion*, 1951, **7**, Apr., 114–117). Test results show the practicability of using magnesium anodes for cathodic protection of underwater areas of active sea-going vessels. The protective current varied between 20 and 35 amp., consumption of magnesium being 435 lb. in 11 months. Suggested improvements are an increased anode/hull-area ratio, use of steel rods cast into the anode, and a vinyl paint system.—E. C.

Economics of Rectifier Installation for Cathodic Protection of a Bare Pipe Line. D. C. Glass. (*Corrosion*, 1951, **7**, Oct., 322–326). This is a description of a rectifier system for cathodic protection for a 10-in. crude-oil pipeline in Texas. It includes design of system, performance data on rectifiers, experience with ground beds, current requirements, bonding procedures with foreign structures, and leak frequency.—H. F.

The Installation and Economics of Placing Magnesium Anodes at Leaks Repaired on a Pipe Line. J. A. Holloway. (*Corrosion*, 1950, **6**, May, 157–161). Magnesium anodes can easily be installed when leaks are repaired and help the anodic areas of the pipe to furnish current for the controlling cathodic areas. Leak-frequency records indicate that a reduced rate of corrosion can be obtained in this manner until complete cathodic protection can be installed.—H. F.

The Value of Backfill with Carbon and Graphite Anodes. J. P. Oliver and W. W. Palmquist. (*Corrosion*, 1950, **6**, May, 147–150). Protection of buried metals against corrosion by impressing a current to make the metal cathodic to its environment is discussed with reference to the use of graphite and carbon anodes. The advantages are increased by the proper selection and use of backfill material. Laboratory and field tests have shown that a suitable carbonaceous backfill will lower the anode-to-soil resistance, prolonging the life of the anode, and transfer the electrochemical attack to the backfill material.—H. F.

Potentials Set up by Thermal Gradients in Iron Immersed in NaCl Solutions. H. H. Uhlig and O. F. Noss, jun. (*Corrosion*, 1950, **6**, May, 140–143). Industrial equipment, such as condenser tubes and boiler tubes, suffers from the galvanic effect of coupling a piece of iron at high temperature to another piece at low temperature whilst immersed in an electrolyte. A laboratory investigation of seamless steel tubing immersed in NaCl has produced specific figures for such action and has shown that decrease of aeration, stirring, or NaCl concentration decreases the potential difference, and that the short-circuit current is limited apparently by polarization of the cathode rather than by the resistance of the electrolyte.—H. F.

Tower Footing Corrosion. W. J. Piper and J. D. Piper. (*Corrosion*, 1951, **7**, Apr., 134–140). A theory of potential measurement for determining corrosion of steel below ground is discussed as applied to underground portions of galvanized steel towers supporting power lines. The measurement of tower to soil potentials is shown to be a rapid and accurate method of ascertaining the condition of the galvanizing below ground. Low potentials were found near switching stations

and near individually earthed steel poles set in concrete. The cause was found to lie in the massive earthing connections of the latter. Disconnection of the overhead earthing wire from the station structure and cathodic protection with magnesium anodes were effective remedies.—E. C.

Interference from Forced Drainage. R. Pope. (*Corrosion*, 1950, **6**, June, 201–207). A method is described for determining the interference which theoretically may be expected on a long structure due to forced drainage of current on a similar parallel structure. The method is applied to 4-in. welded bare steel pipes. Protective coatings have a very marked effect in reducing the interference current, and variations in dimensions of structures, distance between structures, and soil resistivity have relatively little effect on the interference.—H. F.

ANALYSIS

Macroscopic Determination of Hydrogen in Solid Steel. E. V. Podol'skaya and E. G. Shumovskii. (*Zavodskaya Laboratoriya*, 1950, No. 2, 168–173). [In Russian]. The use of vitreous enamel coatings for determining the distribution and content of hydrogen in steel, depending on the appearance of swellings or craters on the coating, is described. Relationships were found between the nature of fracture and the appearance of the corresponding enamel surface.—S. K.

Fusion in the Determination of Carbon in Ferrochromium. M. V. Babaev. (*Zavodskapa Laboratoriya*, 1950, No. 2, 221–224.) [In Russian]. The results of carbon determinations by combustion in ferrochromium standard samples with 2·33 or 5·39% C, using lead and copper, respectively, to cover the charge, are compared. Charge temperatures were varied in the range 1240–1360° C. Copper gives better results than lead.—S. K.

On the Determination of Carbon in Steel and Ferrochromium without Correction. A. G. Bogdanchenko. (*Zavodskaya Laboratoriya*, 1950, No. 2, 217–221). [In Russian]. On the basis of numerous determinations by the Wirtz method for carbon in standard samples of steel and ferrochromium with up to 5·39% of carbon, it is concluded that satisfactory results can be obtained with a single combustion.—S. K.

Quantitative Spectrographic Analysis of Slags. W. J. Price. (*British Cast Iron Research Assoc. J. Res. Development*, 1952, **4**, Apr., 315–335). A short survey of 'rapid' slag control methods and a review of spectrographic methods are followed by a description of the development, and full working details, of the proposed method. In this procedure the metallic oxide constituents of a single slag may be estimated in about 2 hr. The standard deviation is about 3% of the content. (39 references).—B. G. B.

The Determination of Total Sulphur in Coal: Comparison of Eschka and Strambi Methods. I. Davies and P. J. Jackson. (*J. Appl. Chem.*, 1951, **1**, Suppl. Issue 1, s4–s9).

Determination of the Total Sulphur in Coal and Coke by the Reduction Process Supplemented by Rapid Analytical Processes. W. Mantel and W. Schreiber. (*Glückauf*, 1939, **75**, Dec. 2, 929–936). The aim of this work was to find a method of determining the burnt sulphur, by reduction and not by oxidation, comparable with the time spent in the ash determination method. On the basis of the investigations carried out, a new analytical method and its chemical, physical-chemical, and analytical development are described. This new method gives an accuracy of ± 0·03% total sulphur in both coals and cokes and it takes 20–30 min.—R. J. W.

Contribution to Metallurgical Chemistry. P. Klinger. (*Stahl u. Eisen*, 1952, **72**, May 8, 577–580; May 22, 631–635). Recent developments in the following subjects are reviewed: Analytical accuracy, use of beryllium for determining fluorides, balances, emulsifiers, mass spectrometers, and standardization of permanganate with metallic antimony. Details are also given of a large number of analytical procedures used in the steelworks laboratory.—J. P.

Magnetic and Chemical Analyses of Ores and Mill Products Containing Magnetite and Ilmenite. E. Laurila, O. Jantti, and R. T. Hukki. (*Trans. Amer. Inst. Min. Met. Eng.*, 1951, **190**, 797–802). The authors have investigated methods of analysing ores and mill products containing magnetite and ilmenite. The resulting developments described include a diamond-drill core analyser for determining magnetic susceptibility of drill cores containing magnetic materials; a magnetite–ilmenite analyser for pulverized products; and a potentiometric method of determining iron, vanadium, and titanium in succeeding steps from one sample. A continuous and automatic magnetite assay method is also proposed.—G. F.

Notes on the Design, Adjustment and Calibration of Spectrometers. Sir Charles W. B. Normand and R. H. Kay. (*J. Sci. Instruments*, 1952, **29**, Feb., 33–39). Some methods of calibration and adjustment of spectrometers, made possible by the use of high-sensitivity photomultipliers, are discussed. The techniques described include the use of null balancing to measure the intensity ratio of neighbouring wavebands; the use of synchronous commutator rectification of the chopped light signals to discriminate against random fluctuations; new methods of optical alignment; and determination of photomultiplier characteristics.—H. D. W.

A Spectrophotometric Method for Determination of Low Carbon Contents. P. Enghag. (*Jernkontorets Ann.*, 1952, **136**, May, 113–123). [In Swedish]. After burning the sample in oxygen, the SO_2 is removed by chromic acid; the CO_2 is then absorbed in a sodium hydroxide solution containing a *p*H indicator. The decrease in the *p*H concentration caused by the CO_2 is an indication of the carbon content and is measured by a spectrophotometer, Beckman model B. Results achieved when the method was applied to steel and ferro-alloys are presented.—G. G. K.

A New Source for Spectro-Chemical Analysis. A. G. Rouse. (*J. Opt. Soc. Amer.*, 1950, **40**, Feb., 82–84). Some characteristics of a source for spectro-chemical analysis using induction heating in conjunction with a D.C. arc and A.C. spark are described. A study of the boiling point temperature of the sample can be used to differentiate elements and compounds. Examples are given of mixtures $CdSO_4 + H_2O$ with Zn and with $ZnCl_2$.—H. D. W.

The Use of Briquetted Samples in the Spectrochemical Analysis of Carbon and Alloy Steels and Other Metals. J. F. Woodruff. (*J. Opt. Soc. Amer.*, 1950, **40**, Apr., 192–196). A number of spectrochemical methods, developed by the Armco Steel Corp., are discussed; they make use of briquetted powders, drillings, and other cuttings for analysis of steel and zinc. Carbon steels are analysed quantitatively for the common elements and the accuracy of the results is comparable with the routine wet chemical methods.—H. D. W.

The Spectrophotometric Determination of Molybdenum in Molybdenum Steels. P. Karsten and J. H. C. van Mourik. (*Rec. Trav. Chim. Pays-Bas*, 1952, **71**, Feb., 302–308). The existing determination with thiocyanate was modified so that the preservation of Mo^V was certain. This was done by using the combination KI + SO_2 as a 'one-sided' redox buffer. The technique is described in detail, and the maximum concentration of interfering elements before a change in colour intensity of 1–2% occurred in a solution of 3·5 mg. Mo/l. was determined for 15 substances. The method was tested on seven samples of molybdenum alloy steels.—T. E. D.

Colorimetric Determination of Copper in Steels by Means of Rubeanic Acid. A. Lemoine. (*Anal. Chim. Acta*, 1952, **6**, 528–533). [In French]. The rapid colorimetric determination of copper by means of dithio-oxamide or rubeanic acid is described. It can be used for the estimation of copper in an impure state in the presence of iron and alloying elements without first extracting with an acetic buffer medium, the iron complex with citric acid being made. The accuracy is about ± 2·5% on the quantity of copper.—T. E. D.

The Variation with Wavelength of the Spectral Emissivity of Iron and Molybdenum. J. E. Taylor. (*J. Opt. Soc. Amer.*, 1952, **42**, Jan., 33–36). The experimental procedure for the measurement of the emissivity of metal surfaces for wavelengths of between 4000 and 7000 Å., and the methods of preparation of specimens are described. An increase of emissivity with decrease in wavelength was observed, that for iron being greater than that for molybdenum. Differences in the emissivity of the body-centred and face-centred cubic forms of iron are shown.—T. E. D.

Analysis of the Excitation Characteristics of Spectra Emitted by Ferrous Alloys. J. K. Hurwitz and J. Convey. (*J. Opt. Soc. Amer.*, 1952, **42**, Jan., 24–30). Bragg Laboratory low-alloy steel standards and U.S. stainless steel standards were used for the investigation. The excitation potentials of the observed wavelengths of the spectral lines emitted in the low-alloy steel spectrum were calculated. Tentative homologous line pairs were selected on the basis of similar excitation potentials and freedom from interferences. Self-reversal was eliminated at high concentration values by a current of dry air across the discharge, giving a slight increase in precision.

The separate interferences of nickel, chromium, and manganese are shown. (18 references).—T. E. D.

Spectrophotometric Determination of Nickel in Steel with Potassium Dithiooxalate. J. P. Mehlig and B. J. Newby. (*Chem. Analyst*, 1952, **41**, June, 28–31).

HISTORICAL

The Technical University of Stuttgart. E. Siebel. (*Z.V.d.I.*, 1952, **94**, May 21, 393–395). The history of this University is related and particulars are given of the post-war reconstruction plan and the present organization of the teaching.—J. G. W.

Early Use of Mineral Coal in the Chafery of the English Forge. H. R. Schubert. (*J. Iron Steel Inst.*, 1952, **172**, Nov., 313–314). [This issue].

The Iron Industry of Upper Silesia. K. Tanzer. (*Stahl u. Eisen*, 1952, **72**, May 8, 569–574). The history of the Upper Silesian iron industry from early times up to 1945 is reviewed.

Osemund Fining-Forge Iron. F. Schmidt. (*Stahl u. Eisen*, 1952, **72**, Mar. 27, 354–362). The great age of iron production in the Sauerland Mountains, Westphalia, the derivation of the word 'Osemund' from the Latin for 'iron mountain' and the history of the rise and decline of the Osemund industry and the economic conditions prevailing therein are discussed.—J. P.

The Wire Rope Industry. J. Shaw. (*Tisco Rev.*, 1952, **20**, Apr., 10–11). The history of the wire rope industry from 1830 onwards is surveyed. The development from the use of iron wire to the use of steel wire, with its much higher tensile strength, is traced.—T. E. D.

The Blast Furnace in the Early Nineteenth Century. A. Birch. (*Engineering*, 1952, **173**, May 23, 657–658). A detailed description is given of the salient technical developments which contributed to the rapid expansion in the production of iron between 1800 and 1900. Reference is made to the inventors associated with ironmaking, particularly to James Beaumont Neilson.—M. D. J. B.

A Sheffield Firm's Centenary. (*Engineer*, 1952, **193**, Apr. 4, 469–470). An account is given of the history of the Sheffield firm of Samuel Osborn and Co., Ltd., which has reached its centenary.—M. D. J. B.

ECONOMICS AND STATISTICS

New Industrial Regions in the Polish People's Republic. (*Technik*, 1952, **7**, Apr., 160–164). Details of the six-year plan (1950–1955) are given. It is intended to expand the Czestochowa steelworks so as to produce 1·1 million tons of steel per year. The production target for Poland is 3 million tons.—R. A. R.

Developments in Brazil's Iron Industry. E. Tysklind. (*Blad Berg. Vanner*, 1951, No. 1, 197–228). [In Swedish]. An historical survey is made of the Brazilian iron and steel industry since 1500, with special reference to Sweden's influence in its development. Production has increased from 6000 tons wrought iron in 1905 to 532,000 tons steel in 1948. Output expansion problems are discussed. Production could be tripled if sufficient coking fuel could be obtained.—G. G. K.

Steel Production and Consumption Trends in Europe and the World. (*United Nations Dept. of Economic Affairs: Steel Section, Economic Commission for Europe*, 1952, Apr.). Chapter I deals with steel production in Europe, and the view is expressed that sufficient plant will be available for the fulfilment of production plans for 1953. The raw material situation is less certain. The difficulties of making estimates of future steel consumption in Europe are discussed in Chapter II. Chapters III and IV are concerned with trends in steel production in other regions of the world and with the factors likely to affect future steel exports from Europe. Finally, Chapter V considers future problems and possible lines of approach.—T. E. D.

Indian Steel Industry: Its Development in Relation to Industrial Growth. P. Kutar. (*Iron Steel*, 1952, **25**, Apr., 127–128). The author discusses the rôle of the iron and steel industry in India's future industrial development.—G. F.

Steel and the National Economy: A Survey of Current and Future Supplies and Demand. R. M. Shone. (*Iron Steel*, 1952, **25**, Apr., 121–122).

Developments and Trends in Iron and Steel Technology: Some Views of a Swiss Technician. R. Durrer. (*Iron Coal Trades Rev.*, 1952, **165**, July 4, 41–43). The author discusses present developments and possible future trends in the iron and steel industry. The factors considered include direct steel production, low-shaft furnaces, the use of sponge iron as a substitute for pig iron, and oxygen enrichment of the blast in basic converter steelmaking.—G. F.

The French Iron and Steel Industry. H. G. Cordero. (*Metal Bulletin*, 1952, June 13, 13–20). The impressions of a recent visit to the Lorraine and Nord steel areas are given. The visit to Lorraine included the Rombas plants of Sidelor, the De Wendel works of Hayange, Moyeuvre, and Joeuf, and the Sollac continuous strip mill which is nearing completion. Works visited in the Nord region included the continuous strip mill of Usinor at Denain, the steel region around Maubeuge, and the progressive Le Phénix galvanizing plant at Rousies.—T. E. D.

A Look at Sweden's Iron and Steel Industry. T. L. Joseph. (*Amer. Inst. Min. Met. Eng., Blast Furn., Coke Oven, Raw Mat. Conf.*, 1951, Apr., 36–43). A survey of iron-ore production, sintering practice, pig-iron output, and steel production is given.—T. E. D.

Industrial Development in Brazil: National Steelworks' Expanding Output. (*Iron Coal Trades Rev.*, 1952, **164**, June 13, 1299–1300). This article discusses the steadily increasing output of iron and steel in Brazil and the industry's imports and exports which were at a record level in 1951. Proposals for further expansion are indicated.—G. F.

India's Iron and Steel Industry. (*J. Met.*, 1952, **4**, Mar., 252). The present status and expected future developments of the Indian iron and steel industry are summarized.—G. F.

Age of Iron and Steel. Sir Padamj P. Ginwala. (*Metal Market Review*, 1950, Feb., 113–118). A review is made of the development and prospects of the iron and steel industry in India. The undue emphasis in the past on the production of acid steel is criticized.—B. G. B.

Metal Economics. (*J. Inst. Metals*, 1952, **19**, Jan., 225–254). An abridged report is presented of a discussion at a General Meeting of the Institute of Metals in London on 17 October, 1951. The subject was dealt with in two parts: (1) Primary resources of ferrous and non-ferrous metals; and (2) scrap reclamation, secondary metals, and substitute metals. The contributions, following the introduction by A. J. Murphy, were:

The World Supply of Non-Ferrous Metals, Including the Light Metals, R. L. Stubbs (pp. 226–233).

Metals as Natural Resources, S. Zuckerman (pp. 233–234).

World Demand and Resources of Iron Ore, T. P. Colclough (pp. 234–237).

The Scope for Conservation of Metals, Ferrous and Non-Ferrous, C. A. Bristow, A. J. Sidery, and H. Sutton (pp. 240–241).

Economy by Standardization of Alloys and of the Method of Reclamation of Scrap Metals, C. Dinsdale (pp. 241–244).

The Influence of Specifications on Productivity and the Economic Utilization of Ferrous and Non-Ferrous Metals, F. Hudson (pp. 244–245).

Secondary Heavy Metals, E. H. Jones (pp. 245–248).

Secondary Aluminium and Magnesium, W. C. Devereux (pp. 248–252).

MISCELLANEOUS

Recording and Evaluating—Methods and Machines for the Computation of Statistical Data. F. Ferrari. (*Z.V.d.I.*, 1952, **94**, Apr. 1, 274–279). The punched-card system is described, and the various machines are illustrated and explained.—J. G. W.

Armaments Inspectorate Use Statistical Acceptance Methods. (*Machinist*, 1952, **96**, Apr. 19, 577–584). Sampling by attributes is used by the Inspectorate of Armaments, Ministry of Supply. This system, when coupled with revision of inspection layout and equipment on time and motion study principles, is claimed to save up to 90% of the man-hours for inspecting.—E. C. S.

Some Considerations Regarding a Factory Maintenance Engineering Department. M. Bentham. (*Inst. Mech. Eng., Preprint*, Apr. 4, 1952). **Plan Your Factory Maintenance.** M. Bentham. (*Machinist*, 1952, **96**, Apr. 26, 635–639: *Aircraft Production*, 1952, **14**, June, 202–207). The article consists of extracts from a paper on preventive maintenance presented to the Institution of Mechanical Engineers on April 4, 1952. The causes of equipment failures, the investigation of requirements, personnel organization, and maintenance cost control are discussed.—E. C. S.

Reducing Costs and Increasing Production by Preventive Maintenance. D. F. Galloway. (*Machinery*, 1952, **80**, May 1, 782–784). **Reduce Cost Increase Production by Preventive Maintenance.** D. F. Galloway. (*Machinist*, 1952, **96**, May 10, 719–720: *Metallurgia*, 1952, **45**, June, 305–306). The author gives some examples of the savings effected by preventive maintenance.—E. C. S.

The Control of Lifting Gear. G. D. Keeble. (*Mech. Handling*, 1952, **39**, Apr., 157–161). The paper describes a complete card index system worked and used by a large engineering concern for the control of all lifting gear. The keeping of these records not only satisfies Factory Act requirements but aids the maintenance and purchasing departments.—D. H.

Physico-Chemical Studies on Dusts. I—A High-Solubility Layer on Silicious Dust Surfaces. D. W. Clelland, W. M. Cumming, and P. D. Ritchie. (*J. Appl. Chem.*, 1952, **2**, Jan., 31–41).

Physico-Chemical Studies on Dusts. II—Nature and Regeneration of the High-Solubility Layer on Silicious Dusts. D. W. Clelland and P. D. Ritchie. (*J. Appl. Chem.*, 1952, **2**, Jan., 42–48).

Accurate Sheet Mill Weighing. K. A. Blom. (*Iron Steel Eng.*, 1952, **29**, May, 55–62). This paper deals with weighing problems met with in the steel industry. Scale repairs and preventive maintenance are considered.—M. D. J. B.

Metal Stitching. R. H. Warring. (*Machinery Lloyd, European Edition*, 1952, **24**, May, 53–56). The basic advantages of metal stitching over the various other fastening processes are increased production (up to 700% in some cases), material saving (up to 50%), and low cost. Details of the process, the metal wire used, and the type of work carried out are given.

Today's Challenge to Management. W. W. Sebald. (*Iron Steel Eng.*, 1952, **29**, May, 118–120). In this paper the author discusses the human relations problems between labour and management in the American iron and steel industry.

Training of Personnel in Iron and Steel Works. T. Omberg. (*Blad Berg. Vänner*, 1951, No. 2, 315–339). [In Swedish].

Protective Measures for Radioactive Isotopes. V. E. Pullin. (*Engineer*, 1952, **193**, May 16, 660–662).

Amenities in Factory Design. W. R. Bennet. (*J. Roy. Soc. Arts*, 1952, **100**, July 25, 619–634).

Development of Materials for Instrument Manufacture. E. B. Moss. (*Trans. Soc. Instrument Techn.*, 1951, **3**, Dec., 143–156).

Instrument Design for Efficient Vision. B. Gibbs. (*Elect. Times*, 1952, **121**, May, 885–889). The conditions governing the optimum features for indicating instruments are discussed. Limitations in human vision have been overcome by the use of counter-type instruments, rectangular numerals, and by the study of contrast and illumination.—H. D. W.

Plant Management in the German Iron and Steel Industry. E. Gobbers and H. Euler. (*Stahl u. Eisen*, 1952, **72**, Mar. 27, 337–340). The work of the Plant Management Committee of the Verein Deutscher Eisenhüttenleute and, since 1948, of the Industrial Accounting Committee of the Wirtschaftvereinigung Eisen- und Stahlindustrie on the problems relating to increases in productivity and living standards is reviewed by their respective chairmen.—J. P.

Human Relations In the Open Hearth. R. Smith. (Amer. Iron Steel Inst.: *Blast Furn. Steel Plant*, 1952, **40**, May, 531–534). The author discusses the relationship between workers and management.—B. G. B.

Human Relations in Industrial Operational Research. A. W. Swan. (*Engineer*, 1952, **194**, July 25, 117–120).

Management's Responsibility for Training. A. R. Stewart. (Engineering Inst. of Canada and Canadian Industrial Trainers' Assoc.: *Eng. J.*, 1952, **35**, July, 729–731, 733).

Development and Utilization of Engineering Talent. J. C. McKeon. (*J. of Engineering Education*, 1952, **42**, Apr., 388–391).

The Place of Cooperative Education in Modern Industry. C. R. Osborn. (*J. of Engineering Education*, 1952, **42**, Apr., 406–411).

The Statistical Interpretation by Means of Punched Cards of Original Data for Materials Management in a Large Steelworks. H. Rübmann. (*Stahl u. Eisen*, 1952, **72**, Mar. 27, 351–353). Punched-card evaluation of data from a blooming mill has clarified the effect of various factors on rolling temperature and indicated certain operating rules.—J. P.

The Evaluation of the Results of Measurements by Frequency Methods. P. H. Oidtmann. (*Glückauf*, 1952, **88**, May 24, 518–525).

Organizing Company Research Activities. D. A. Bell. (*Times Review of Industry*, 1952, **6**, July, 10–11).

Safety and Industrial Hygiene in the Laboratory. H. H. Fawcett. (*Chem. Eng. News*, 1952, **30**, June 23, 2588–2591).

The Prevention of Crane Accidents. W. Kölz. (*Von Roll Werkzeitung*, 1952, **23**, May, 73–74).

Treatment of Silicosis—Chemical Inhalation Assisted by Electrostatic Field. E. Rosenthal. (*Elect. Rev.*, 1952, **151**, July 18, 121–122).

Steelworks Ventilation: Principles and Some Installations. L. G. Davies. (*Iron Steel*, 1952, **25**, Mar., 98–101). The author discusses means of improving ventilation in existing steelworks in which plant density is high and which are under cover of old buildings.—G. F.

Mechanized System Launches New Era for Literature Searching. (*Chem. Eng. News*, 1952, **30**, July 7, 2806–2808).

BOOK NOTICES

BRANDENBERGER, E. "*Grundriss der allgemeinen Metallkunde.*" 8vo, pp. 333. Illustrated. Basle, 1952: Ernst Reinhardt Verlag. (Price 13.50 Swiss francs).

In his introduction Professor Brandenberger claims that his approach to his subject is intended to fall between the empiricism of the engineer and the rigour of the pure scientist. There can be no doubt that this approach has produced a worthy addition to elementary metallurgical literature. Aided by a large number of well-produced diagrams, the author has concentrated a very wide knowledge of metals and alloys into some three hundred pages. The treatment is entirely non-mathematical and has a lucidity remarkable in a purely descriptive work.

The first part of the book deals with the structure and properties of pure metals. A well-illustrated and tabulated section on the crystal structure of pure metals is followed by a short but clear exposition of the simpler ideas relating to the electron theory of metals. Phase changes in pure metals are treated in considerable detail, and it is pleasant to record that diffusionless transformations are given adequate attention. It is unfortunate that the diagram illustrating isothermal transformations should be inaccurate; however, the text does not repeat the error. The deformation of single crystals and polycrystals is discussed, and this section leads to a reasonably satisfactory account of recrystallization and recovery. It is worth noting that the idea of dislocations is ably expounded, and that 'polygonization' is mentioned in the section on recrystallization.

The second part is concerned with substitutional solid solutions. The influence of alloying on the physical properties of metals is treated briefly. The extent of primary solubility receives but little attention, the subject being dealt with by listing the Hume-Rothery rules. There can be no doubt that the most satisfactory section in this part of the book is the treatment of the ordering process. Both long-range and short-range order are discussed, and the many diagrams help to give the reader a clear picture of the principal types of superlattice. Binary equilibrium diagrams are discussed in this part of the book and the important features of a number of binary systems are presented in tabular form. Polymorphic transitions in binary systems receive adequate attention. Precipitation from solid solution is also treated and this leads to a useful section on age-hardening.

The third part of the book deals in a similar way with interstitial solid solutions. In this part, as might be expected, the iron–carbon and iron–nitrogen systems receive particular attention. Some attempt is made to discuss the behaviour of ternary solutions in which both substitutional and interstitial atoms are present; this treatment is not wholly satisfactory, because the author understandably avoids the problem of ternary equilibrium diagrams.

The fourth part of the book considers intermediate phases. Here a vast amount of information is gathered together and neatly classified. The diagrams of crystal structures are very well produced. The factors governing the formation of intermediate phases are discussed at some length. The physical and mechanical properties of intermediate phases receive some attention and the section ends with a discussion of the properties of polyphase alloys.

The last part has the title "Chemical Reactions of Metals." At first sight this seems rather out of place in a book of this type, but, in fact, the subjects discussed are very much the concern of the metallurgist. Following a general treatment of heterogeneous chemical reactions, the author gives sections on reactions of metals with solids, between metals and gases, and between metals and liquids. In this way, such matters as sintering, scaling, and corrosion are treated in a unified manner. The book ends with a section on stress corrosion.

The book can be recommended unreservedly; it is a great pity that, being in German, it will have less use than it deserves among English students.—J. H. WOODHEAD.

HOSELITZ, K. "*Ferromagnetic Properties of Metals and Alloys.*" Monographs of the Physics and Chemistry of Materials. 8vo, pp. xii + 317. Illustrated. Oxford, 1952: Clarendon Press. (Price 40*s*.)

This is an extremely good book and Dr. Hoselitz merits congratulation for the manner in which he has collected and presented as a lucidly coherent whole a most impressive amount of information on the properties of ferromagnetic metals and alloys. The volume under review is the eleventh of a series of monographs on the physics and chemistry of materials, which is intended to summarize the subjects in a form useful "to physicists in universities and in Government and industrial laboratories." The metallurgist should not, however, infer that since the series is intended for specialists in another field this book has little interest for him.

The book may be reviewed in two parts. The first part deals with the magnetization curve and the methods used in magnetic measurements. The author does not go into any great detail about the atomic theory of ferromagnetism, but the modern theory of the magnetization curve for ferromagnetics is described at length and is excellently presented. Methods of measuring magnetic constants are reviewed and the magnetic analysis of alloy systems is described in some detail. There is a short and fairly elementary chapter on metals and alloys in general which is brief but adequate as an introduction to the subject to the physicist who may be metallurgically ingenuous.

The second part of the book is a most valuable compilation of the types and properties of ferromagnetic metals and alloys, dealt with as magnetically soft and magnetically hard materials respectively. Most of the specially developed magnetic materials are here with details of compositions, production methods, and special treatments applied to them. A noteworthy omission, however, is powder-core materials, which receive surprisingly little mention.

To each chapter is appended an excellent bibliography and the subject index survived all the tests to which the reviewer subjected it. A commendable and well-produced book.—A. H. SULLY.

KOCH, J. J., R. G. BOITEN, A. L. BIERMASZ, G. P. ROSZBACH, and G. W. VAN SANTEN. "*Strain Gauges, Theory and Application.*" Philips Technical Library. 8vo, pp. viii + 95. Illustrated. Eindhoven, Holland, 1952: N.V. Philips' Gloeilampenfabrieken; London: Cleaver Hume Press, Ltd. (Price 15*s*.)

This book is published jointly by the Netherlands Industrial Organization for Applied Scientific Research and Philips Industries, with a view to providing a comprehensive and practical guide to the use of wire resistance strain gauges. It is clearly written, logically arranged, and well illustrated. Although the title and text use the unqualified term 'strain gauges', the book is concerned only with wire resistance strain gauges and particularly with their application to measurements on structures.

The opening chapter discusses the common form of the wire resistance strain gauge, and details the properties of the wire, the paper on which it is wound, and the adhesive, which are necessary to ensure satisfactory performance; this discussion of the theory of operation of the gauge helps the reader to obtain a clear appreciation of the limitations and difficulties involved and precautions to be taken in the application of these gauges.

Concerning measuring instruments and apparatus for use with resistance gauges, the classical techniques are described, such as the direct deflection galvanometer and manually adjusted null-balance bridge for D.C.-excited strain-gauge bridges and the well-known A.C. amplifier and associated bridge circuits for carrier-excited bridge systems. The authors wrongly infer that no high-gain D.C. amplifiers suitable for D.C.-excited strain-gauge bridges are available commercially; many well-known automatic potentiometric recorders and feedback amplifier systems are now in use for resistance strain-gauge work. A.C.-excited strain-gauge bridge systems and means of reducing errors due to cable resistance and capacities and contact resistances, are well described.

A complete chapter is dedicated to "Techniques of Cementing and Connecting Strain Gauges" and forms a very valuable guide to the user of these gauges, particularly for outdoor applications on structures. The surface preparation, gauge fixing, and protecting processes are described in detail, which is fully justified by the supreme importance of this stage of the work if reliable measurements are to be made. A chapter on the interpretation of strain-gauge readings gives a comprehensive account of the various potential sources of error in resistance strain-gauge measurements, such as tolerances in the gauge factor, connecting lead and contact resistances, moisture in gauges, gauge temperature coefficients, etc. A useful table lists these sources of error and quotes the approximate magnitudes of the errors to be expected from them.

A chapter on "Stresses and Theories of Failure" defines commonly used terms such as principal stress and principal plane, plane and linear stresses, and strain energy, and derives them from fundamental stress theory. The mathematical treatment included is, however, unnecessarily complex; for example, the treatise on three-dimensional stresses could easily have been condensed, as it is not normally used in strain-gauge measurements. The two-dimensional stress theory could have been much simplified; several theories of failure and the design criteria developed from them are discussed, but the one most commonly used in Great Britain is not stated. Formulæ are given for the calculation of principal stresses and strains from the three linear strain measurements normally obtained from a system of strain gauges attached to a surface, and simple graphical methods of obtaining solutions according to these formulæ are given. Altogether the chapter is too theoretical for the practical man and would have benefited considerably by more attention to the graphical methods of analysis of strain-gauge readings.

A chapter is devoted to the use of resistance strain gauges in measuring instruments. The examples given include load cylinders for weighing and varied force-measuring problems, extensometers, gas and liquid differential pressure meters, and accelerometers. Unfortunately, there is no mention in this chapter of special high-temperature strain gauges, unbonded gauges, and alternative adhesives such as Bakelite and Araldite cements, which are being used in instruments employing these gauges.

The book provides a useful guide to the inexperienced user of wire resistance strain gauges. It is, however, largely concerned with applications in structural engineering, and the apparatus described is limited to that manufactured by Philips Industries. Its scope could, with advantage, have been broadened to include descriptions of the much wider range of apparatus now available to the user of wire resistance strain gauges. Such an extension would have helped to justify the very comprehensive title.—S. S. CARLISLE

THOMPSON, HENRY. "*Steel Defects and their Detection.*" 8vo, pp. viii + 84. Illustrated. London, 1952: Sir Isaac Pitman and Sons, Ltd. (Price 15*s*.)

The initial chapter commences with an elementary description of an ingot, and goes on to describe some types of ingot structure formed during solidification. The description has been abbreviated to such an extent that it means very little. Chapter II comprises an elementary and

incomplete description of the common defects normally associated with ingot manufacture. It is doubtful whether the type of reader who finds this book useful will know a Widmanstätten structure when he sees one. The next chapter gives a relatively good description of many of the defects arising during rolling. Chapter IV discusses macro-examination and gives various recipes for macro-etching, most, if not all, of which have been recorded many times in existing publications. The following chapter contains some useful practical hints and tips on the preparation of microsections. Chapter VI describes the microconstituents of steel and is well illustrated by photographs, although items such as the difference between troostite and sorbite cannot be visualized except from practical experience. The final chapter discusses special tests and describes the apparatus and methods used. In parts, these descriptions are reminiscent of a maker's catalogue, and must be completely incomprehensible and unnecessary to the reader who bought this book because he thought it was devoted to a discussion of steel defects.

To summarize, this book contains much of interest to the beginner in a works metallographic laboratory. Consisting, as it does, essentially of abstracts from other publications, it contains little that is new. If these abstracts had been so selected as to be especially valuable to one class of reader, then some merit in the preparation of this book would have been apparent. If those responsible for detecting and classifying defects in a steelworks were dependent upon the information given in this publication, their work would be very unsatisfactory; in fact, however, all modern works will have much more comprehensive descriptions to give their trainees.—A. JACKSON

TRIGG, C. F. "*An Engineer's Approach to Corrosion.*" 8vo, pp. ix + 133. Illustrated. London, 1952: Sir Isaac Pitman and Sons, Ltd. (Price 15*s.*)

This book gives a picture of the corrosion and protection of metals as seen by an engineer who has had experience of corrosion damage in practice and of the problems involved in rectifying it. The mechanism of corrosion is explained in simple terms, some effects are described with particular reference to deterioration in mechanical properties, and, finally, surface preparation and protective treatments are outlined.

The author's experience and reading appear to be fairly wide, but most of the literature references are to papers in engineering journals and no papers which have appeared in the *Journals* of The Institute of Metals or The Iron and Steel Institute are mentioned. Within its limitations the book offers a preliminary introduction to the subject, and should prove useful to the engineers for whom it is intended and who are often ultimately responsible for applying preventive and remedial measures.—T. A. BANFIELD

NEW PUBLICATIONS

AMERICAN FOUNDRYMEN'S SOCIETY. "*The Engineering Properties of Cast Iron.*" 8vo, pp. 93. Illustrated, Chicago, Ill., 1950: The Society.

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